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CERAMIC REGENERATOR SYSTEMS DEVELOPMENT PROGRAM

PROGRESS REPORT FOR PERIOD OCT.1, 1976 TO SEPT.30, 1977

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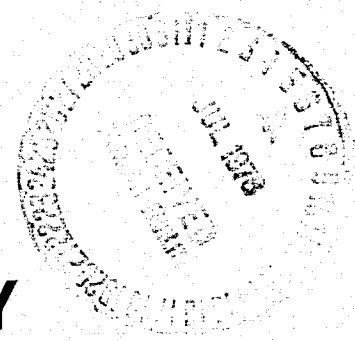
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16. Abstract The primary objective of the DOE/NASA Ceramic Regenerator Design and Reliability Program is to develop ceramic regenerator cores that can be used in passenger car gas turbine engines, Stirling engines, and industrial/truck gas turbine engines. The major cause of failure of early gas turbine regenerators was found to be chemical attack of the ceramic material. Improved materials and design concepts aimed at reducing or eliminating chemical attack were placed on durability test in Ford 707 industrial gas turbine engines late in 1974. This report describes the results of 19,600 hours of turbine engine durability testing accumulated during the period from October 1, 1976 to September 30, 1977. Two materials, aluminum silicate and magnesium aluminum silicate, continue to show promise toward achieving the durability objectives of this program. A regenerator core made from aluminum silicate shows minimal evidence of chemical attack damage after 6935 hours of engine test at 800°C (1472°F) and another shows little distress after 3510 hours at 982°C (1800°F). The results obtained during this period in ceramic material screening tests, aerothermodynamic performance tests, stress analysis, cost studies, and material specifications are also included in this report.			
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SUMMARY

Since the NASA/Ford Ceramic Regenerator Program is organized by tasks, the results obtained during the October 1, 1976 to September 30, 1977 period will also be summarized by Task.

Task I — Core Durability Testing at 800°C (1472°F)

Approximately 19,600 hours of engine durability test (39,200 core hours) were completed from October 1, 1976 to September 30, 1977 on cores made from chemically resistant materials and mounted on a rim support and drive system.

Turbine engine durability tests on aluminum silicate regenerator cores tend to confirm the results of the accelerated chemical attack engine tests, which showed that this material was relatively impervious to chemical attack. Eight cores of this material have each accumulated over 4000 hours of engine test at 800°C (1472°F) and one core has attained 6935 hours of engine test with a minimal amount of dishing, which is an indication that no significant chemical attack has occurred.

A single high expansion, rim-mounted MAS core accumulated 5389 hours at 800°C (1472°F). The thermal cracks formed in the rim of the matrix during the first 200 hours are stable and will not propagate unless the operating temperature is raised above 800°C (1472°F).

One of Supplier A's thin-wall AS cores has now accumulated 4268 hours of engine test. This weaker construction appears to be able to withstand the seal shoe rubbing forces without serious wearing or chipping. Elastomeric bonding of the ring gear to this material requires additional development, since separations in the elastomer-matrix bond region have occurred on all thin-wall cores bonded using the conventional technique. Utilization of a high-compliance elastomer system shows promise of solving this problem, and one such configuration has now accumulated 2314 hours of engine test without distress.

The cement holding the hub inserts in place failed in five out of the eighteen AS cores that were engine tested. Hub repair with a low temperature cement has proven to be inadequate and this approach has been abandoned for AS material. A hub configuration which utilizes a high temperature cement and a solid ceramic ring shows potential. There have been no failures in the six cores of this design that are now on test, and two such cores have each accumulated over 2300 hours.

Task II — Core Durability Testing at 1000°C (1832°F)

About 3510 hours of engine test at an average regenerator inlet temperature of 982°C (1800°F) have been accumulated on a thick-wall aluminum silicate core. The aluminum silicate core shows no signs of thermal or chemical attack damage after this exposure.

Supplier A's CPD-6 core failed after 2307 hours of exposure to the same test conditions. Failure was due to chemical attack and this material has now been phased out of the engine test program.

A thin-wall AS core has replaced the failed CPD-6 core, and it has now accumulated 1182 hours at an average inlet temperature of 982°C (1800°F).

Task III — Material Screening Tests

The materials screening test portion of the ceramic regenerator development program is well under way. Preliminary data from the laboratory testing program (hot and cold face simulation) suggest that many of the new MAS and AS materials may be better than current material candidates. Accelerated corrosion testing of the first core containing matrix inserts has progressed to the half-way point, and a subsequent chemical analysis of 25 and 39 engine hour specimens will indicate the degree of sodium attack experienced by 4 candidate materials.

Task IV — Aerothermodynamic Performance

A total of twenty-six matrix fin configurations have been evaluated at the present time. Fifteen rectangular, eight sinusoidal, two isosceles triangular and one hexagonal configuration comprise the present matrix sample size.

Aerothermodynamic performance characteristics can be significantly altered by variations in fin shape uniformity, bond quality, and surface roughness. Based on the present matrix sample size, up to 25% variation in overall fin efficiency can occur due to manufacturing process limitations.

Utilizing overall fin efficiency (J/F) as a basis for comparison, and considering matrix fin configurations with equivalent hydraulic diameter, material thickness, and flow length, the effect of fin geometry can be summarized as follows:

1. The sinusoidal triangular structure appears to be the least efficient fin configuration. An increase in heat transfer efficiency accompanied by an increase in pressure drop can be obtained by reducing the aspect ratio of this type of surface geometry to approach the extruded isosceles triangular structure.
2. Isosceles triangular surfaces appear to be equivalent to square configurations provided the manufacturing process quality is equal.
3. For embossed rectangular structures, heat transfer and pressure drop characteristics increase with increasing aspect ratio.
4. Additional data is required before the hexagonal flow passage can be compared to the square and triangular structures.

Task V — Design Studies of Advanced Regenerator Systems

Matrix mechanical properties (radial MOR and radial compressive strength) were evaluated for a thin-wall aluminum silicate core. It was determined that the radial compressive strength of the thin-wall matrix is substantially less than the radial MOR.

A three-dimensional finite element stress analysis of several configurations of stress relieved regenerators indicated that stress relief slots substantially reduce tangential stress in the rim of a regenerator between the slots.

A preliminary test evaluation indicates that with the exception of the inner seal cross-arm, current state of the art seal coatings appear to be acceptable for 1200°C (2192°F) regenerator inlet temperature. Additional testing is required to establish the upper temperature limitation of the cross-arm coating.

A more refractory material, such as silicon nitride, may be required for regenerators where the inlet temperature exceeds 1100°C (2012°F) or 1200°C (2192°F). Increased axial length and hydraulic diameter would have to be incorporated into re-

generators of these materials to control pressure loss and still compensate for the axial conductivity loss inherent with these high conductivity materials.

Task VI — Ceramic Thermal Stability Tests

The high temperature dimensional stability testing of ceramic regenerator materials has been started. The testing programs at 1000°C (1832°F) both with and without sodium are well underway with four test materials and a 9455 LAS standard making up the first test batch. Testing at the higher temperatures has been started in a very limited way; a material of high interest (AS) was chosen to initiate stability tests at 1100°C (2012°F) and 1200°C (2192°F) without sodium present. New materials have been received, and preparations for testing have been completed.

Task VII — Manufacturing Cost Studies

A relatively current (circa 1976) study of the various costs incurred in the production of a ceramic heat exchanger is in existence. The study, representing the combined efforts of Ford and a number of ceramic producers experienced in the fabrication of honeycomb matrix shapes, will serve as a nucleus of information to be periodically updated reflecting technical innovation as well as increasing costs.

Task VIII — Core Material and Design Specifications

Two ceramic core materials and core design specifications now exist: one for an operational temperature of 800°C (1472°F) and the other, comprised of bits of design and testing data, for 1000°C (1832°F) core temperature. The lower temperature specification is quite comprehensive; however, advancing technology and improved understanding have contributed to the obsolescence of this specification. An update is in order, and this revision is being carried out. The higher temperature specification is quite preliminary, and future attention will dwell on extending the scope of this specification.

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INTRODUCTION

Since 1965, Ford Motor Company has been engaged in developing a ceramic regenerator system for use in gas turbine engines. Over 100,000 hours of engine operating experience have been accumulated on a sample of approximately 1,000 regenerator cores fabricated of lithium aluminum silicate (LAS) and produced by two suppliers.

By 1971, when Ford started limited production of the 707 series industrial gas turbine engine, it was believed that the regenerator system had a durability life of at least 4000 to 5000 hours. Because of unexpected sporadic failures in applications such as generator sets and pleasure boat installations, a new series of controlled durability tests were started. When these tests were terminated in August, 1973, 11 core failures had occurred out of a sample of 30 cores on test. It was determined that the failures were primarily caused by a severe chemical attack on the lithium aluminum silicate material used. This test data showed that these regenerators had a B₁₀ life of 600 hours and an average life of 1600 hours.

Late in 1973, an engineering research program was initiated to solve the regenerator core failure problem. The primary objective of this program is to develop ceramic regenerator cores that can be used in passenger car gas turbine engines, Stirling engines, industrial/truck gas turbine engines and other industrial waste heat recovery systems. Specific durability objectives are defined as achieving a B₁₀ life of 10,000 hours on a truck/industrial gas turbine engine duty cycle at a regenerator inlet temperature of 800°C (1472°F).

In late 1973 Ford funded Corning Glass Company, Coors Porcelain Company, GTE Sylvania, Inc., and the W. R. Grace Company to develop ceramic regenerator cores which would have acceptable thermal stresses, good resistance to chemical attack, and meet the life objectives. In 1974, several new materials, including aluminum silicate (AS) and magnesium aluminum silicate (MAS), were screened in laboratory and engine tests and found to have acceptable resistance to chemical attack. Regenerator cores made from new materials were placed on durability test late in 1974 and early in 1975.

The Ford 707 industrial turbine is being used as the test bed to evaluate these new regenerator materials and concepts. During 1974, over 18,000 engine test hours (36,000 core hours) were accumulated on regenerator systems. In 1975 and 1976, 18,200 and 15,100 engine test hours (36,400 and 30,200 core hours) were logged for a total accumulation over the three-year interval of 102,600 core hours. Core durability testing will continue in 1977 and 1978 in an effort to demonstrate the B₁₀ life of 10,000 hours required for an industrial gas turbine engine regenerator.

Late in 1974, the Alternate Automotive Power Systems Division of the Environmental Protection Agency joined with Ford Motor Company in an "Automotive Gas Turbine Ceramic Regenerator Design and Reliability Program." In early 1975, this program was transferred to the newly-formed Energy Research and Development Administration (ERDA). When the ERDA/FORD Contract was completed September 30, 1976, the National Aeronautics and Space Administration (NASA) was given technical and administrative responsibility for this program.

In 1977 a DOE/NASA cost-sharing program was initiated jointly with Ford Motor Company to continue the ceramic regenerator design and development work that was started under the original EPA/FORD contract. This latest program is subdivided into eight major tasks. These tasks are:

- Task I — Core Durability Testing at 800°C (1472°F)
- Task II — Core Durability Testing at 1000°C (1832°F)

- Task III — Material Screening Tests
- Task IV — Aerothermodynamic Performance
- Task V — Design Studies of Advanced Regenerator Systems
- Task VI — Ceramic Thermal Stability Tests
- Task VII — Manufacturing Cost Studies
- Task VIII — Core Material and Design Specifications

The technical progress in each of these tasks for the period from October 1, 1976 to September 30, 1977 is recorded in the following sections of this report.

DISCUSSION OF RESULTS

TASK I. CORE DURABILITY TESTING AT 800°C (1472°F)

I.A. INTRODUCTION

Section E of Reference 1 described the engine test results obtained by Ford Motor Company on ceramic regenerators used in the 707 turbine engine up to the end of 1973. These data were obtained primarily on LAS regenerator cores mounted at the hub and driven through ceramic pins cemented into the rim. These data showed that chemical attack was the major cause of failure, and that this type of regenerator core configuration would have a B₁₀ life of 600 hours and a B₅₀ life of 1600 hours. Section M of Reference 1 described the test results obtained in 1974. Much of the data presented were on the hub mounted LAS core, with a continuous rim containing stress relief slots, and driven by a ring gear which was bonded to the core with an elastomeric material. This configuration, which showed a durability improvement, had a B₁₀ life of 1100 hours and a B₅₀ life of 3200 hours. A B₁₀ life of 1100 hours was obtained from a Weibull Analysis of the failures in this sample, and means that 10% of the regenerators of this configuration will fail in less than 1100 hours of engine test. A B₅₀ life of 3200 hours means that 50% of the regenerators will fail in less than 3200 hours of engine exposure.

Section P of Reference 1 described the engine test results that were obtained in the first six months of 1975. This test program had concentrated on evaluation of the rim support system, and cores made from several different chemically resistant materials. By this time only a few LAS cores were being evaluated. These were mounted with a rim support system, and they were being carried along in the program so that a comparison could be made between the durability record of the hub-mounted and rim-supported systems. All other cores being tested were made from chemically resistant materials and utilized the rim-support mounting system.

Section E of Reference 2 described the engine test results obtained during the last half of 1975 and the first three quarters of 1976. These tests showed that rim-mounted LAS regenerators with very high safety factors achieved a B₁₀ life of 2350 hours and a B₅₀ life of 3600 hours. It is believed that this is close to the maximum durability that can be expected from an LAS regenerator system. These tests also indicated that LAS cores with a protective coating or those made of Supplier A's CPD-6 material did not have the potential of achieving the desired objectives and they were phased out of the program. AS and MAS cores showed promise in both accelerated and long-term engine durability tests. An AS core had accumulated 5600 hours and a MAS core 2200 hours at the end of this report period.

This report describes the results of the additional engine tests that were conducted on these same concepts from Oct. 1, 1976 to September 30, 1977. During this period, 19,600 engine test hours (39,200 core hours) were accumulated, bringing the total accumulation of engine test hours since the start of the test program on January 1, 1974 to 65,700 hours (131,400 core hours).

I.B. STATUS

I.B.1 Durability Record of Aluminum Silicate Regenerators

To date 17 different aluminum silicate (AS) regenerators, fabricated by Supplier A, have been engine tested in the Ford 707 turbine. While all these cores contain the same material they can be broken down into two classifications depending upon their fin geometry and wall thickness. The original AS configuration was a thick-wall matrix containing about 900 holes per square inch. As part of a program to fabricate higher performance rotary heat exchangers, Supplier A developed a thin-wall, aluminum silicate material. The thick-wall aluminum silicate has an average matrix wall

thickness of 0.11 mm (.0043 inch) while the thin-wall has a thickness of 0.07 mm (.0026 inch) and contains 1300 holes per square inch. A performance comparison, which is based on shuttle rig test results, shows that the thin-wall matrix will have an effectiveness at part power that is about 1-2% higher than that of the thick-wall matrix. This could be translated into about a 5-10% gain in part power fuel economy when a thin-wall regenerator is operated in the 707 turbine engine.

Ten thick-wall AS cores are being tested at 800°C (1472°F) under identical operating conditions and these cores make up the control sample on which durability projections will be based. The durability status of these cores is shown in figure I. B. 1. 1. All cores have accumulated at least 500 hours of engine durability. Their record is compared in this figure to that of the large sample of LAS cores that was evaluated previously in the same type of engine test. The LAS cores in this sample were hub-mounted, while all of the aluminum silicate cores tested to date have been rim-mounted. The LAS sample is based on 23 regenerators and over 25,000 core hours of engine durability as described in Section M of Reference 1. The B_{10} and B_{50} lives for this sample are 1100 hours and 3200 hours, respectively, and are based on several core failures. This is shown on the left hand side of Figure I. B. 1. 1. About 43,000 core hours of durability have been accumulated on the aluminum silicate sample with the highest hour core having attained 6935 hours. At least two failures are required before a Wiebull Analysis can be undertaken. There have been no failures of cores made from this material, so a B_{10} or B_{50} life projection cannot be made. From a visual comparison of the LAS and AS engine test history shown in this figure, it is obvious that the AS cores have a vastly improved durability record.

Six thin-wall AS cores have also been engine tested at 800°C (1472°F) as shown in Figure I. B. 1. 2, and one core has accumulated over 4268 hours. Difficulty has occurred in maintaining consistent elastomeric bonding of the ring gear to the thin-wall core, and this problem has limited test hour accumulation on this material. This problem is discussed in detail in Section I. B. 6 and Section V. B. 3.

Since the thin-wall AS material has 1/3 the strength of the old LAS material, there was some concern that a regenerator seal system that had been developed for the stronger LAS material would damage the surface of the thin-wall AS material. A comparison of the core surface wear under the peripheral seal rubbing shoe for both thick and thin-wall AS cores is shown in figure I. B. 1. 3. It is believed that, with the Ford seal shoe peripheral coatings, most of the core wear occurs when foreign debris is trapped or passes between the rubbing shoe and the core. Such debris may consist of powdered insulation that has eroded off the main housing insulation blankets, other ceramic dust, or rust. This debris embeds itself in the softer, cold-side rubbing shoe coating, and as a consequence no core wear is observed on this face. The debris cannot embed itself in the harder, hot-side rubbing shoe coating and will cause core surface wear in this location. The large scatter in the hot-side data reflects the random pattern in which wear occurs as well as measurement difficulty. Within the accuracy of these results it appears that the thin-wall core does not exhibit a significantly higher surface wear under the peripheral rubbing seal than does the thick-wall core.

Because of the core "dishing", which will be discussed in the next section, it is almost impossible to precisely measure core surface wear due to the cross arm rubbing shoe. The measurements taken to date would suggest that neither the thick nor thin-wall cores show any measurable sign of surface wear due to the cross arm seal. While more data are needed, the thin-wall material appears to have enough integrity to withstand normal seal shoe rubbing forces.

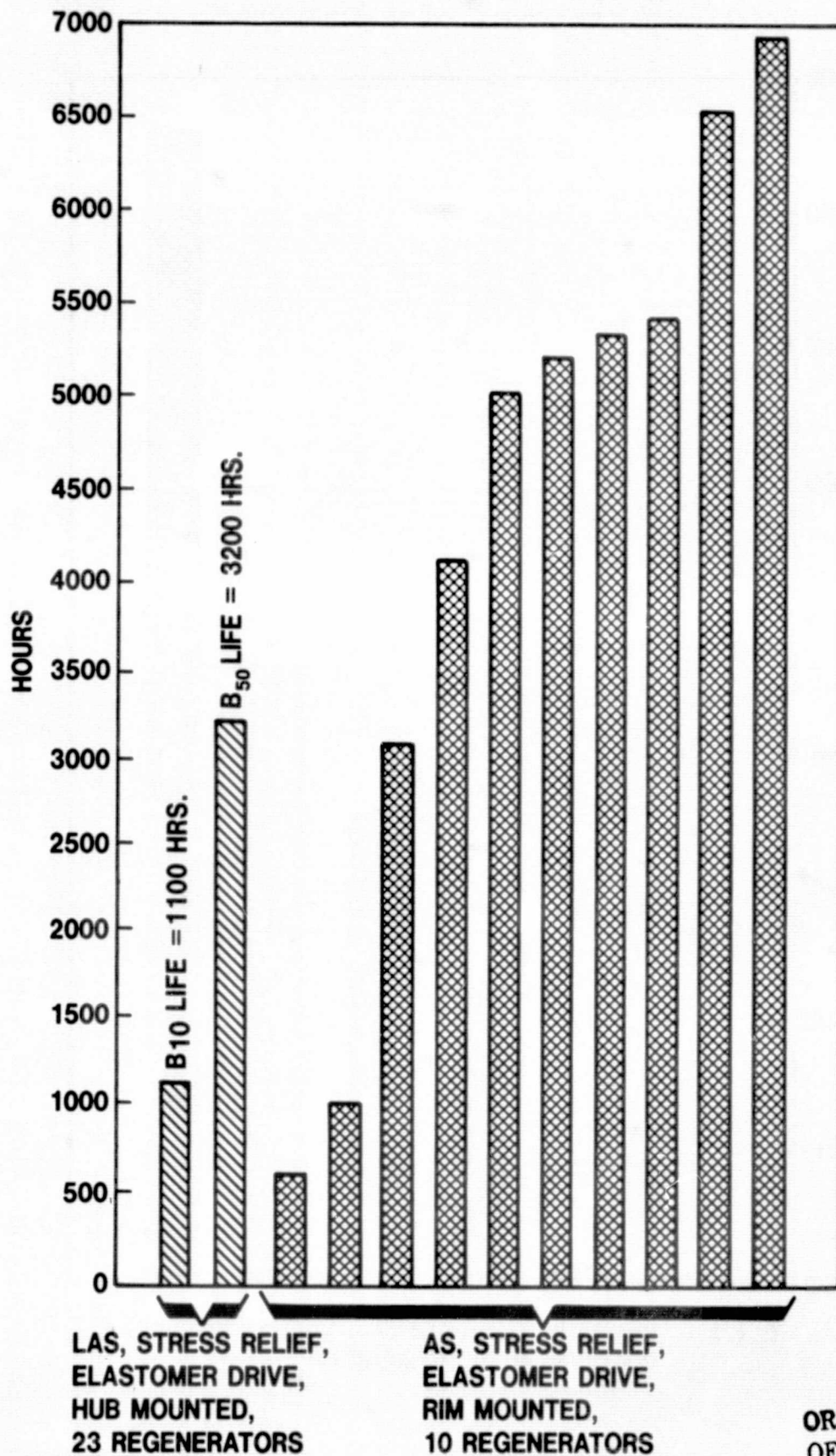


Figure I.B.1.1

Durability Record of Thick-Wall AS Regenerators
Operating at 800°C (1472°F)

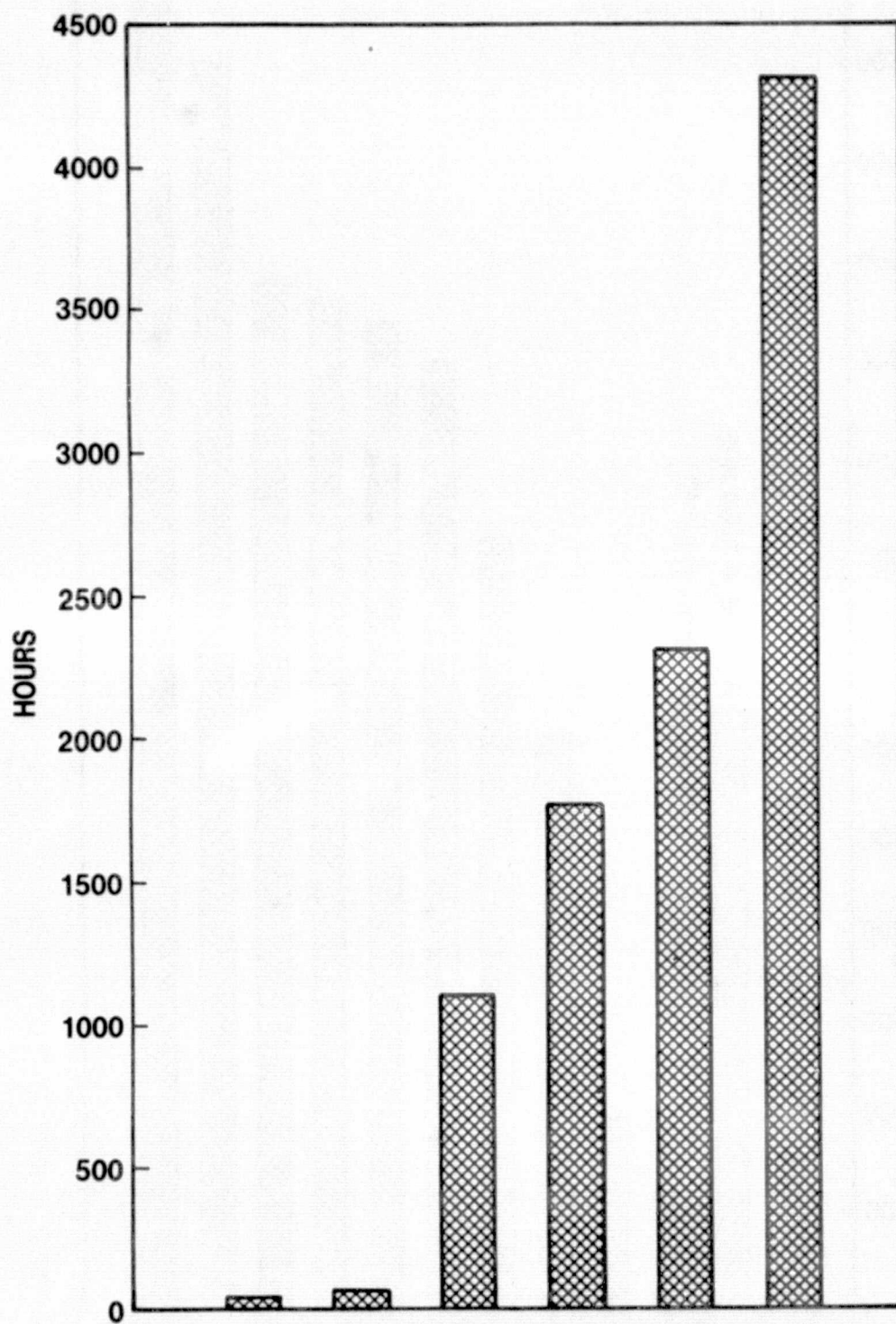


Figure I. B. 1. 2 Durability Record of Thin-Wall AS Regenerators Operating at 800°C (1472°F)

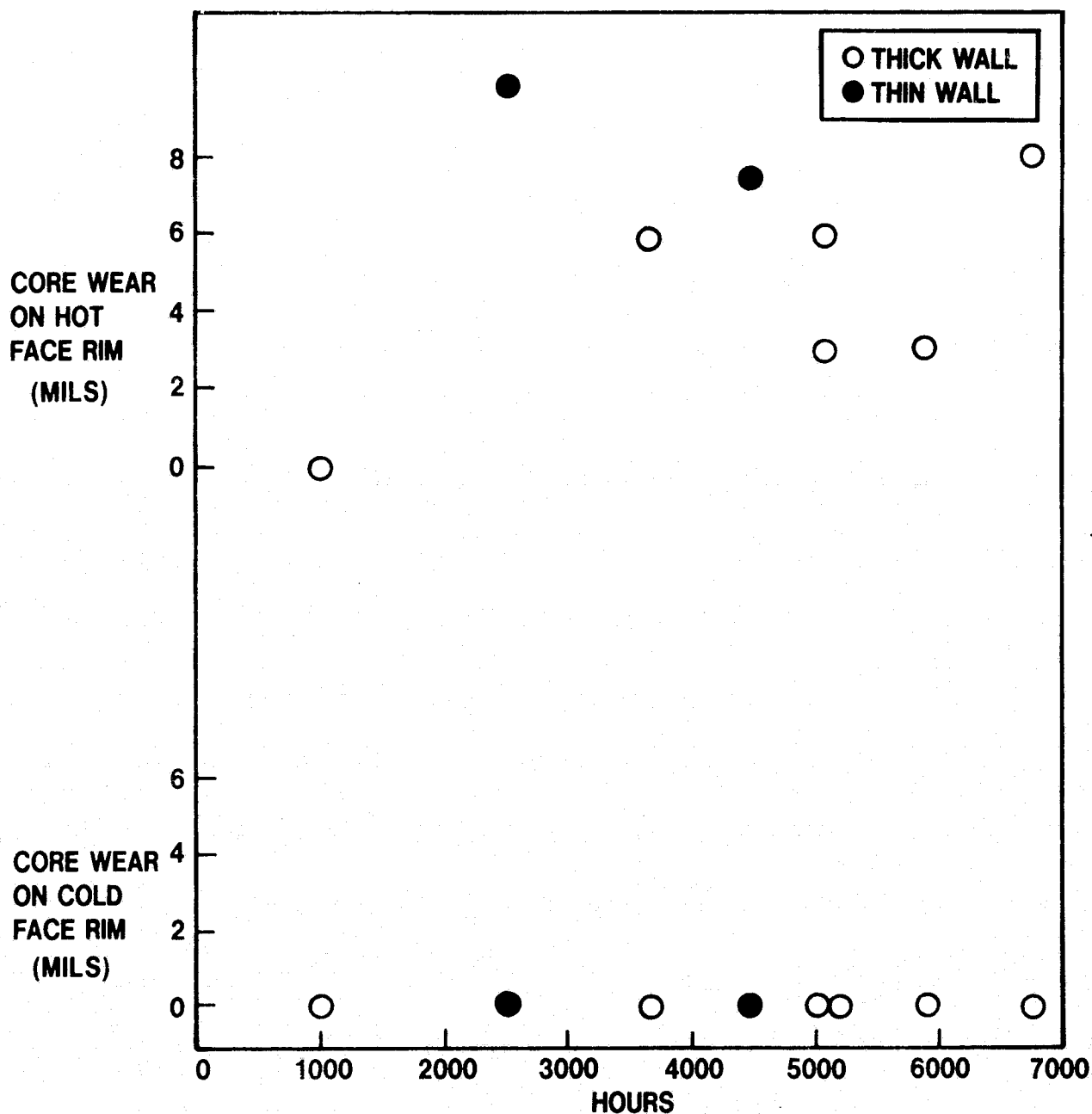


Figure I.B.1.3 Surface Wear of AS Regenerators vs. Operating Hours in an Engine

Long-term durability tests on the thick-wall AS cores show that this material has very good resistance to chemical attack and the thin-wall material is expected to behave in a similar manner. The running history of all of Supplier A's AS cores that have been engine tested are shown in Figure I.B.1.4. This figure also includes the cores tested at 1000°C (1832°F) and described in Section II.B. Almost 57,000 hours of engine test have been accumulated on this material. None of these cores show any serious signs of thermal distress or chemical attack damage. To date a total of eight AS cores have each accumulated over 4000 hours of engine test without visual distress, so it appears that this material has the potential of achieving the program objectives.

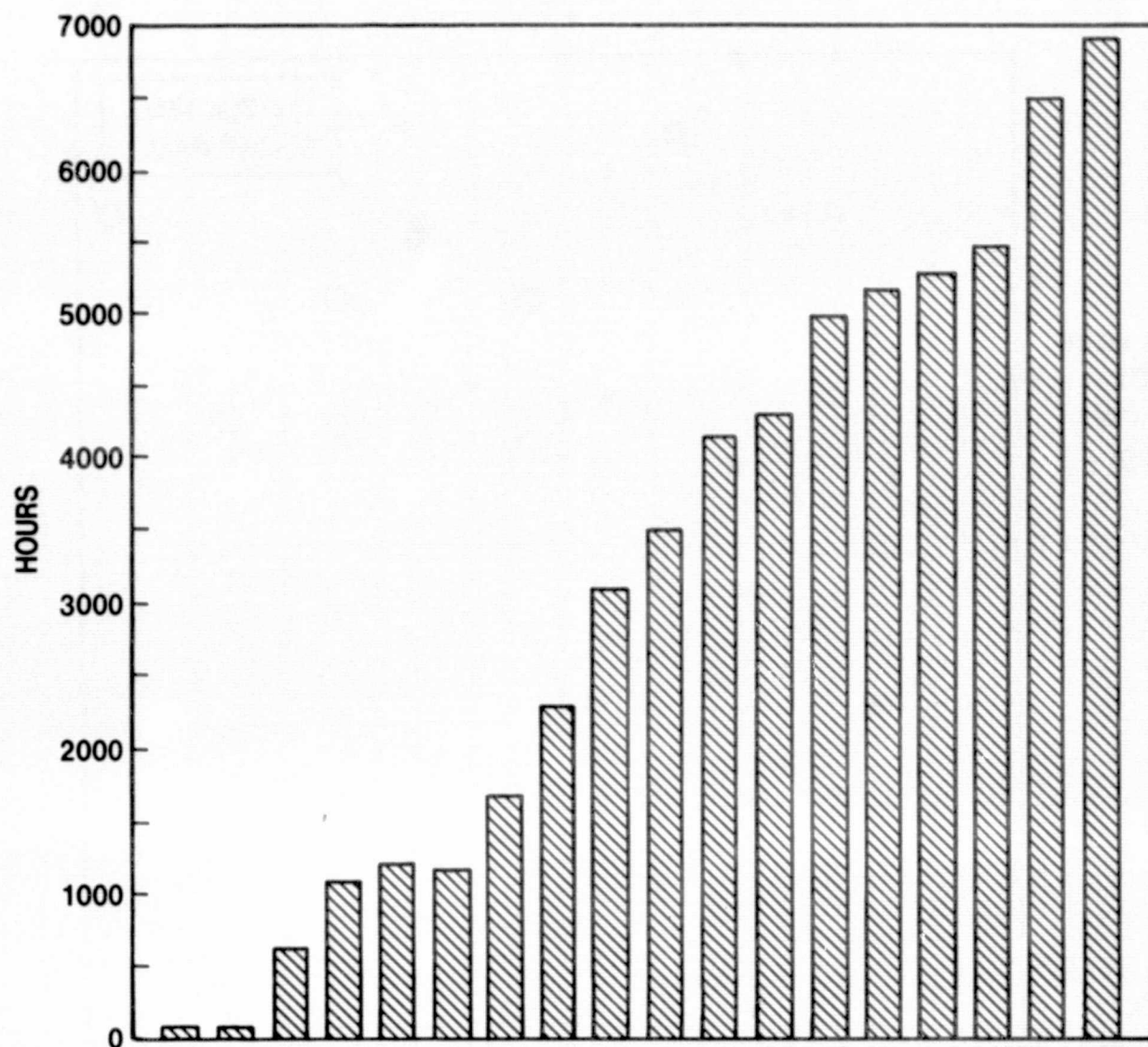


Figure I.B.1.4 Durability Record of All AS Regenerators Engine Tested in the Ford 707 Turbine

I.B.2. Chemical Attack Measurements on AS Regenerators

None of these aluminum silicate cores exhibit any of the signs of serious distress which are typical of chemical attack. Two such signs are: the chemical composition remaining in the core after service and the amount the core has distorted or dished in its cold or free state. Chemical attack will cause lithium leaching or a change in the chemical composition on both the hot and cold side of the core. This lithium oxide loss or composition change in turn will cause a change in the crystal structure of the material resulting in a change in thermal expansion behavior and the generation of microcracks. As a consequence, one face of the core will grow more than the other, causing a dish or out-of-flatness in the core when it is cold.

The nine high-hour, thick-wall aluminum silicate cores were examined for chemical attack by measuring their dishing or out-of-flatness in the free state. These values, along with the engine hours at which the measurement was made, are shown in Figure I.B.2.1. LAS regenerators usually dish 15 to 25 mils in 2,000 hours

before they fail. All nine AS cores show little distortion which suggests that they have undergone little if any chemical attack damage. Since one core has accumulated 7000 hours and only shows 8 mils dishing, these results are very encouraging. Although more engine durability time is needed to support the conclusions reached in the accelerated engine tests, it would appear that the aluminum silicate cores are relatively insensitive to chemical attack.

In the next quarter, durability testing of these AS cores will continue and, if possible, additional aluminum silicate cores will be placed on test. It is planned to keep several cores of this material on engine test throughout 1977 and 1978 to acquire long-term durability data.

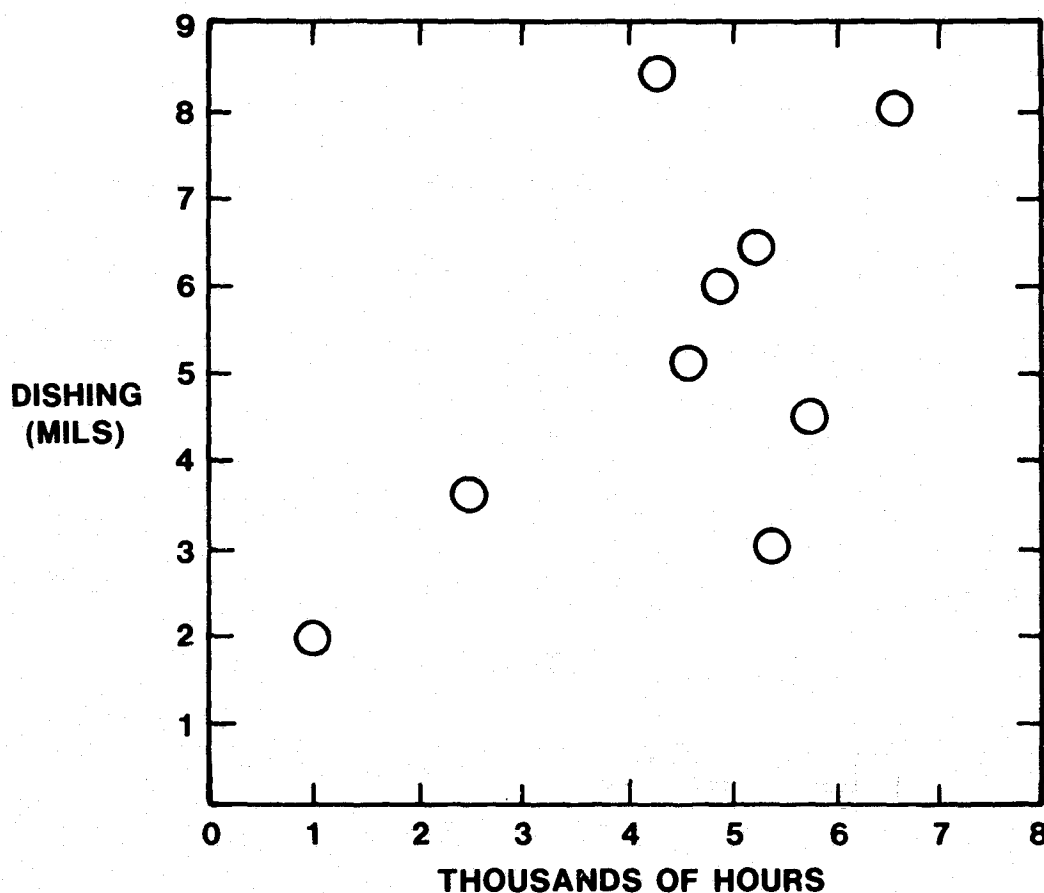


Figure I.B.2.1 Dishing History of Thick-Wall AS Cores

I.B.3. Suspended Tests of Certain Cores

In determining the statistical life of a component utilizing the Weibull theory, both failed and operational components are considered in the analysis. The operational or "suspended" components, as they are called in statistical analysis, can be of two different types: those components which are still running when the testing is terminated and those components which have been failed or damaged accidentally by an external source. In this latter category are components that have been damaged by failures of the test equipment itself. In this regenerator program cores which have failed because of mechanical loads, thermal stresses or chemical attack are considered to be true failures. There have been no such failures of AS or MAS regenerators. Cores that have been damaged as a result of mishandling or because of a failure somewhere else in the engine are considered "suspended" items and will be treated

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as such in the Weibull Analysis. Four such suspensions occurred during this report interval.

A power turbine failure occurred in engine 506 and the two cores, No. 32 and No. 45 were damaged by debris. Only the surface was damaged in Core No. 45 (Figure I. B. 3. 1.) and it was returned to service. A cone-shaped hole (Figure I. B. 3. 2.) was punched in core No. 32 by high velocity debris. Attempts to patch this hole and return the core to service resulted in a complete breakup of this core (Figure I. B. 3. 3). Testing of core No. 32, which was a thick-wall AS core and had accumulated 6935 hours, was terminated, therefore, by the accidental failure of a power turbine.

Testing of another core, No. 53, was terminated when this core was damaged in shipment. This core was being returned from the Supplier after having its hub repaired (this problem is discussed in Section I. F.) when it was broken in transit. Core No. 53 was a thin-wall AS core, and had accumulated 1064 hours of engine test.

Core No. 34 was terminated at 5188 hours when cracks which were formed during rebonding of the hub propagated to the rim and caused a segment to jam under the crossarm seal (Figure I. B. 3. 4). The hub cement first failed in this core after 4750 hours, causing the hub to come loose and creating three radial cracks in the matrix. This type of failure is attributed to improper processing during fabrication and is not considered a true core failure. This failure mode and the corrective action are discussed in Section I. B. 5 of this report. An experimental hub held in place with three ceramic pins was then cemented into place in this core. It failed after an additional 38 hours of engine test, and this failure caused an additional growth of the three cracks initiated during the first hub failure. A third hub, incorporating a ceramic ring, was then cemented in place and the matrix cracks propagated almost to the rim during this processing. After 400 hours of additional engine test, these cracks allowed a segment to shift axially, jamming a crossarm seal.

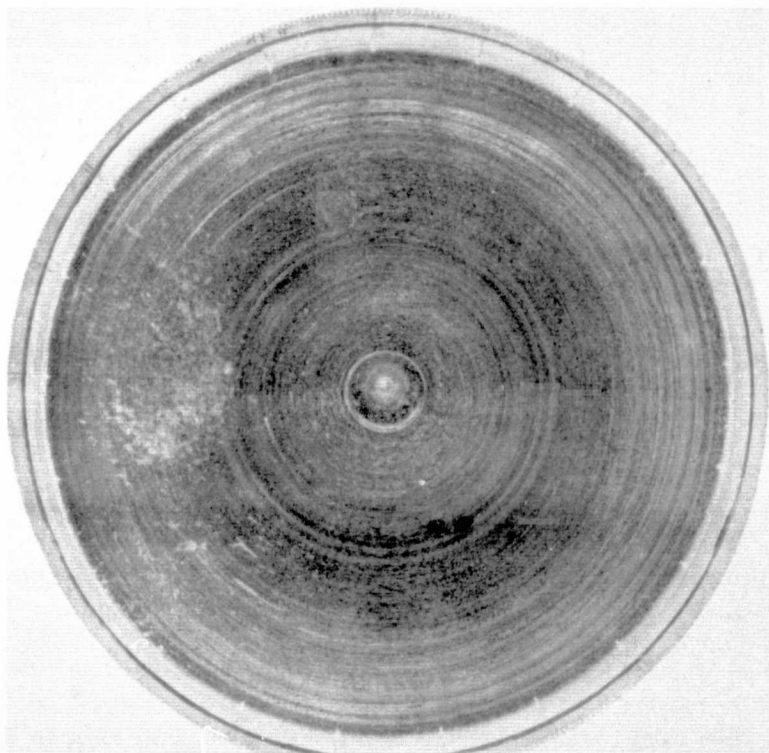


Figure I. B. 3. 1 AS Core No. 45 Showing Surface Damage from Power Turbine Failure

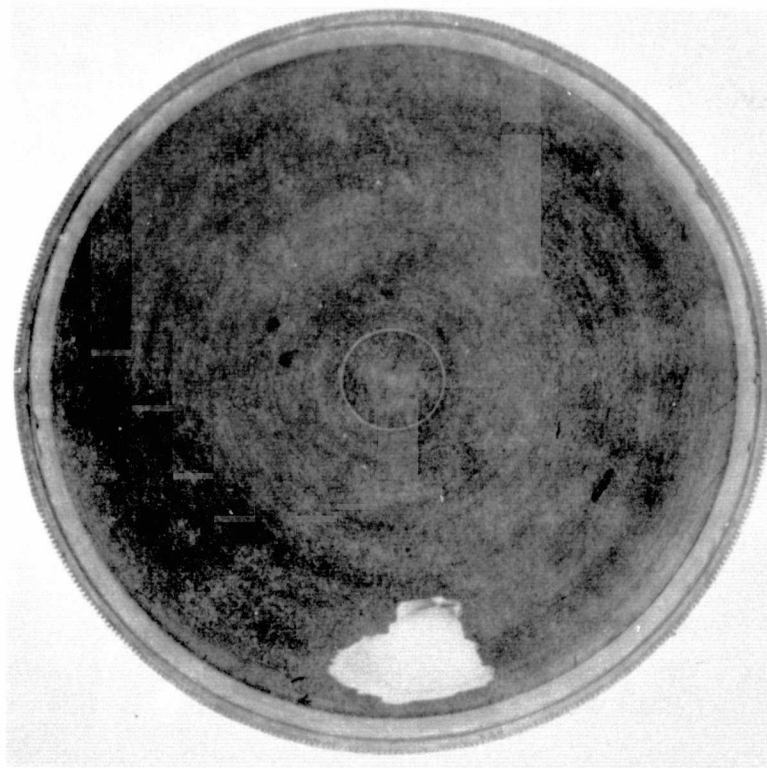
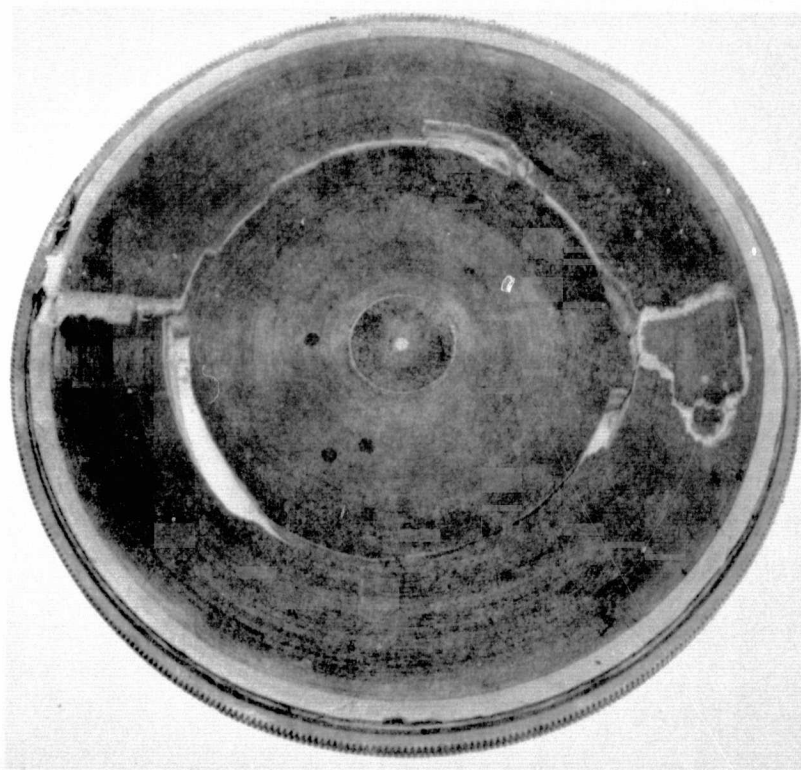


Figure I.B.3.2 AS Core No. 32 Showing Hole Made in Core by Debris from Power Turbine Failure



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Figure I.B.3.3 AS Core No. 32 Showing Final Failure of Core Initiated in Region Damaged by Power Turbine Failure

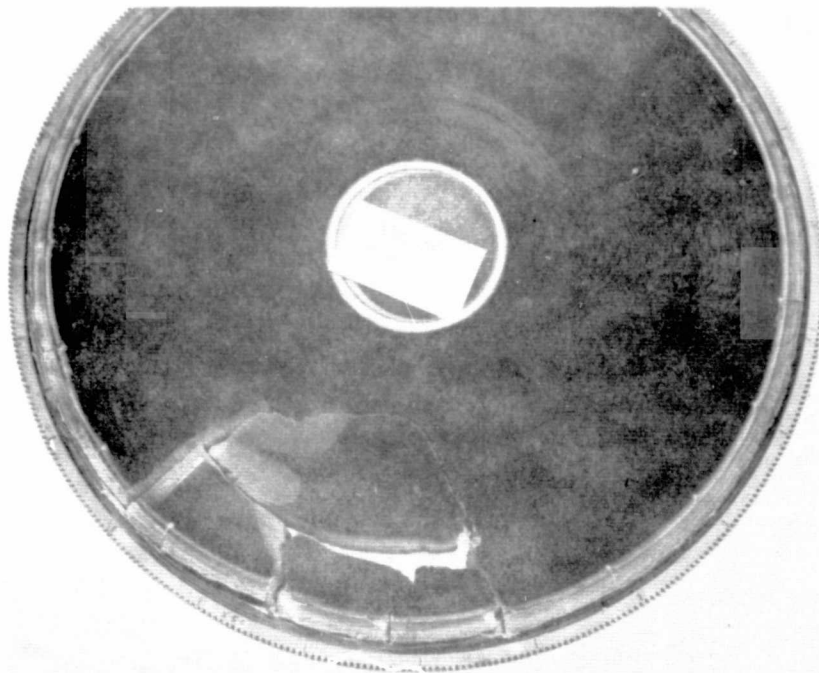


Figure I.B.3.4 Thick-Wall AS Core No. 34 Which Failed Due to Cracks That Were Initiated During Rebonding of Hub

Core No. 47 was installed backwards in an engine. Since the ring gear extends closer to the cold face than the hot face of the core, this improper installation resulted in the ring gear striking the main housing when the engine was started. The axial load imparted to the core by this housing-ring gear interference caused the core to fail (Figure I.B.3.5). The similarity between this failure and the axial load failure shown in Figure A.2.2 of Reference 1 is apparent.

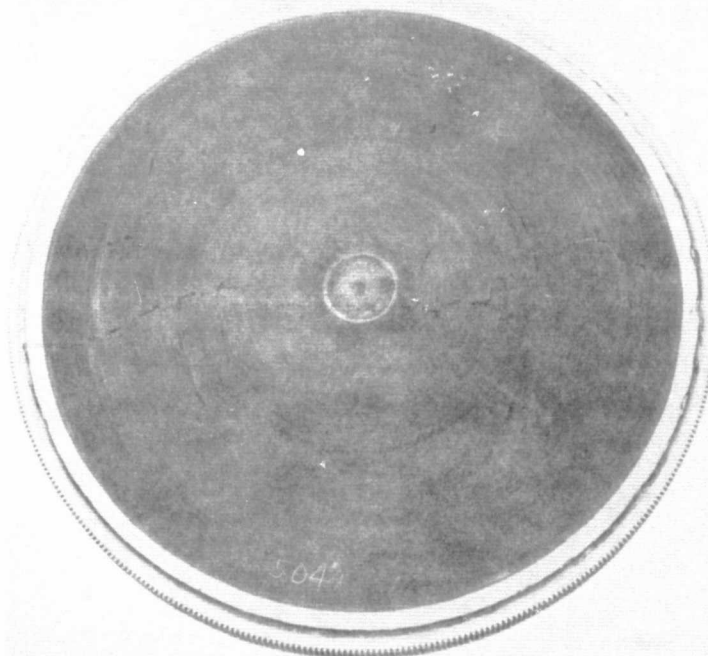


Figure I.B.3.5 Thick-Wall AS Core No. 47 Damaged Due to Improper Engine Installation

I.B.4 Durability Test of a Magnesium Aluminum Silicate Regenerator

Engine tests of two early MAS regenerators are described in Section M of Reference 1. One of these early MAS cores accumulated 426 hours of engine test before it was retired. The testing of this core was terminated when the cement holding the hub or center section in place failed and caused damage in this region. Subsequently, an improved procedure for bonding the center insert to the rest of the matrix was developed. This procedure was used to cement the center insert into a MAS core fabricated by Supplier D. This core, which is shown in Figure I.B.4.1 was evaluated in both the accelerated chemical attack engine test and long term engine durability tests (Reference 2). It has now accumulated 5380 total hours of engine test.

The core had developed thermal stress cracks in the space between the stress relief slots. These cracks, which were noted after 200 hours of testing, are shown in Figure I.B.4.2. The cracks are attributed to thermal stresses, because the MAS material used in this core has a relatively high coefficient of thermal expansion and the core was operated at temperatures up to 800°C (1472°F). The analysis conducted in Section V.B.2 of this report on this same high expansion MAS material with a slotted rim showed that at these temperatures the rim thermal stress safety factor could be substantially below unity. The material in the rim area, therefore, would be expected to fail and develop thermal cracks. These cracks propagated into a region below the regenerator seal shoe inner diameter, where the ceramic is in a state of compression, and then they stabilized.

After 1552 hours on the MAS core, a safety device caused the engine to shut down automatically because of an overtemperature which was caused by clogged engine air inlet filters. On teardown inspection, it was found that several of the rim cracks in the MAS core had propagated radially inward. These cracks can be seen in the photograph taken after the emergency shutdown and presented in Figure I.B.4.3. An examination

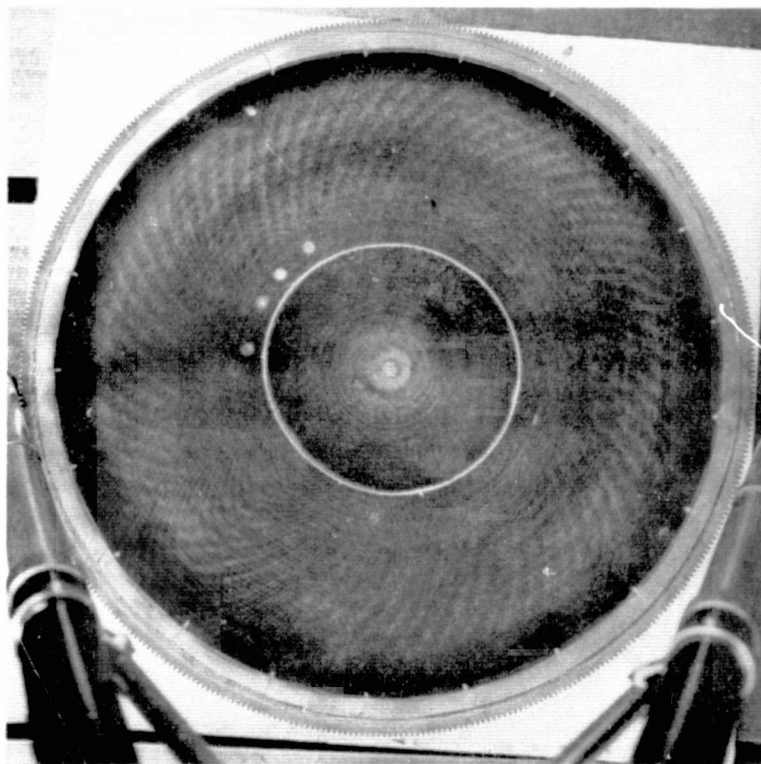


Figure I.B.4.1 MAS Regenerator

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of the recorded engine data showed that the cores operated at progressively higher temperatures, in excess of 850°C (1562°F), for the last 25 hours as the filters clogged. These higher temperatures increased the thermal stress and reduced the rim safety factor an additional 15 to 25%, so that it was well below unity. Apparently the added thermal strain induced in the rim of this MAS core caused the original cracks to propagate.

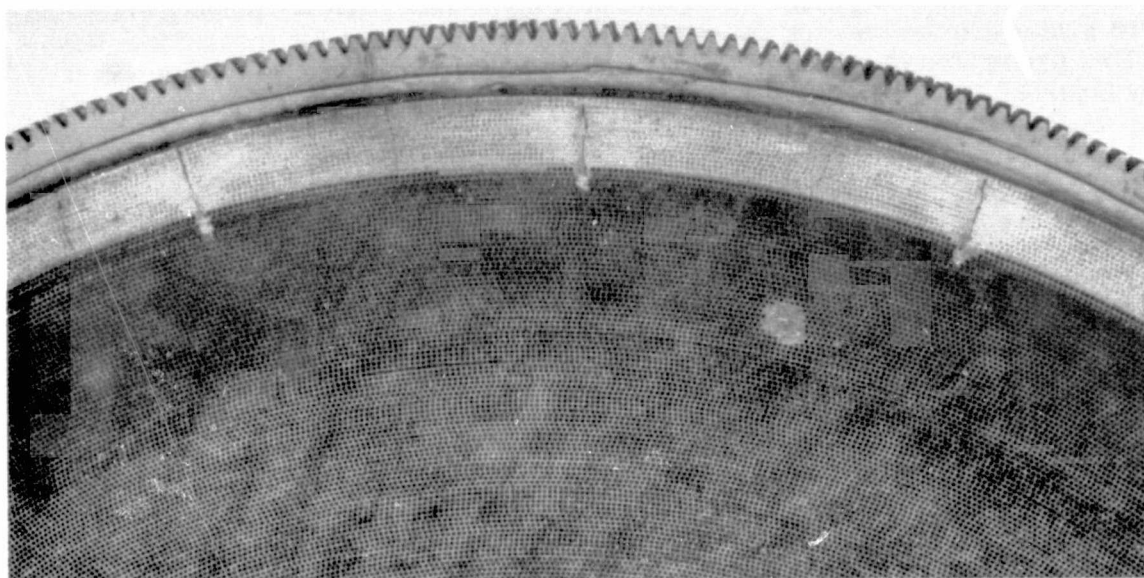


Figure I. B. 4. 2 MAS Regenerator Rim Showing Thermal Stress Cracks Located Between Stress Relief Slots After Operation at 800°C (1472°F)

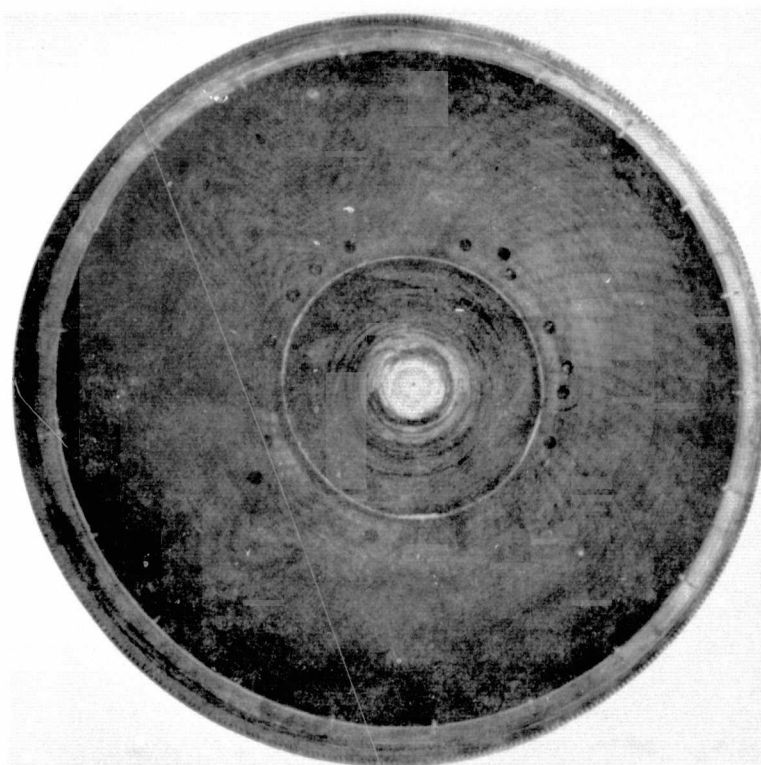


Figure I. B. 4. 3 Thermal Stress Cracks in MAS Core After Operation at 850°C (1562°F)

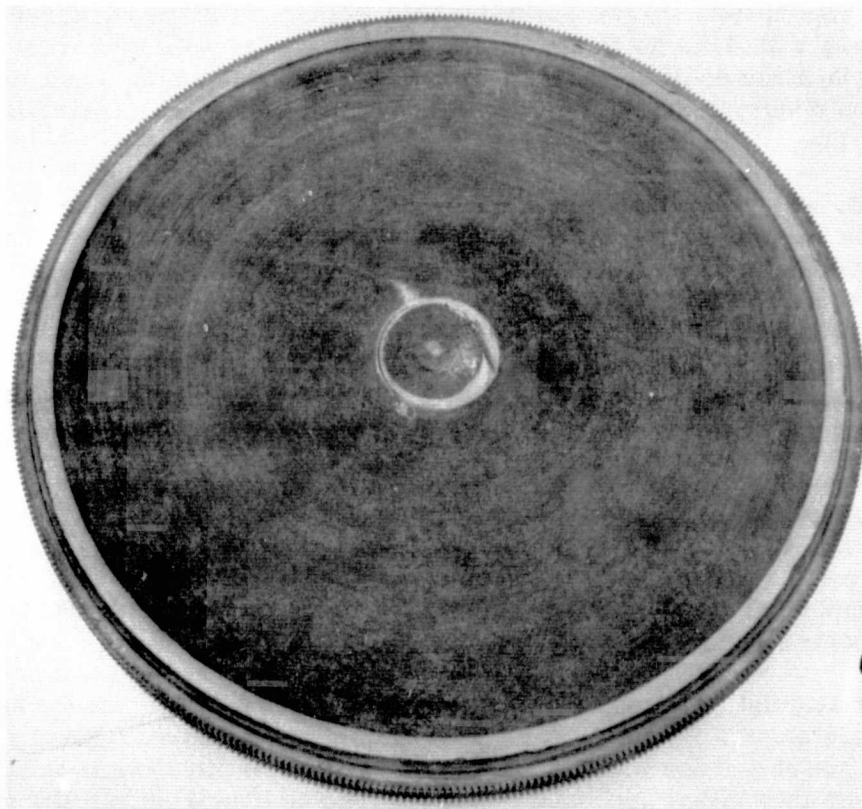
Even with elongated cracks, the regenerator was still operational and it was returned to service. It accumulated an additional 3828 hours at 800°C (1472°F) by the end of the report period for a total engine life of 5380 hours. As described in the next section of this report, the hub of this core came loose due to a cement failure after 3752 hours, but the hub was repaired and the core returned to service.

The data obtained to date from this single high expansion MAS core suggest that this material has the potential for good durability at temperatures up to 800°C (1472°F) providing it is properly stress relieved.

It is planned to place more of these MAS cores on durability test in the near future. Analysis presented in Section V.B.2 of this report shows that more advanced and sophisticated thermal stress relieving techniques will allow the present MAS material to be operated at 800°C (1472°F) with higher safety factors. In addition, four different suppliers have been concentrating on fabricating MAS regenerators using more advanced materials with lower expansion characteristics, so that the regenerator can be operated in a higher temperature environment with current stress relief techniques. It appears that with some additional development, MAS has the potential of achieving the program objectives at 800°C (1472°F).

I.B.5 Hub Cement Failures

During the third quarter of 1976, the cement holding the hub insert in place failed in three different thick-wall AS cores. These cores had accumulated 68 hours, 1939 hours, and 2368 hours at the time of failure. Photographs of the 2368 hour failure and the hub insert are shown in Figures I.B.5.1. and I.B.5.2. In each case the failure was attributed to improper composition or improper processing of the cement itself.



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Figure I.B.5.1 AS Core No. 43 After Failure of Cement Bonding Hub Insert in Place

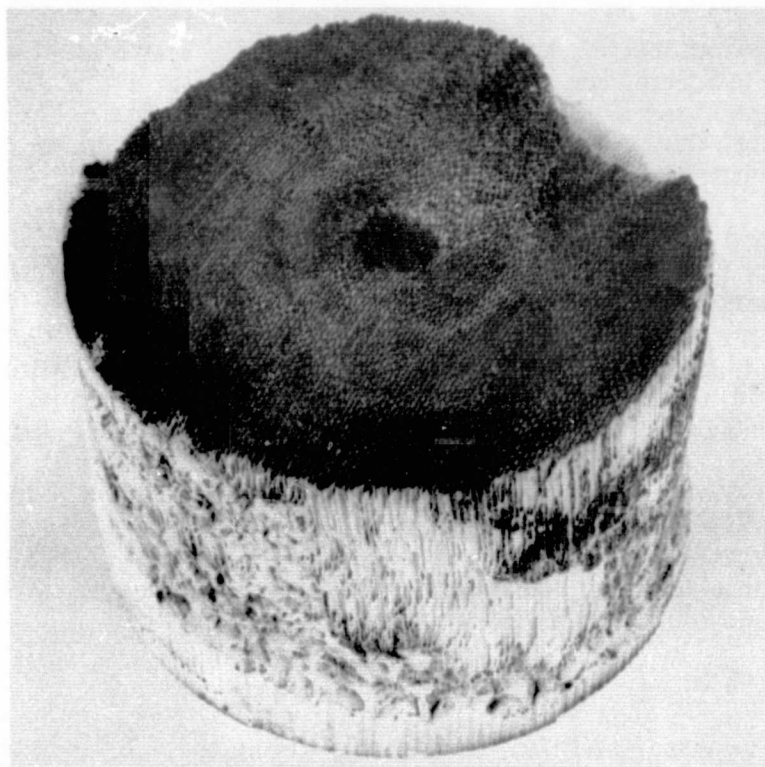


Figure I.B.5.2 Hub Insert from Core No. 43 with Non-Foamed Cement in Center

During this report period, two additional hub cement failures occurred. One was a thick-wall AS core with 4750 hours and the other was a thin-wall AS core with 1064 hours. All five failures contained the same cement and processing procedure. Thus five failures have occurred in the eighteen AS cores that have been tested in the Ford 707 turbine.

This cement, which was developed specially for use with AS material, relies on a foaming action or expansion when it is fired to properly bond the two ceramic pieces together. An examination of Figure I.B.5.2 shows that this foaming action occurred at both faces of the insert, but did not take place in the center. The cement actually shrank away from the ceramic in the center portion of the matrix. The insert was held in place, therefore, by a thin band of cement at each core face. As the engine housing distorts or creeps with operating hours, slight additional seal rubbing forces are applied to this section of the matrix. These slight additional forces probably caused this thin band of good cement to fail.

In an attempt to return these cores to service without removing the ring gear, the hubs were cemented in place with a low temperature cement. Figure I.B.5.3 shows a thin-wall AS core with a new hub held in place with this low temperature cement. This approach was abandoned when engine test experience showed that 500 hours was the best that could be expected with this procedure.

As a further attempt to increase the strength of the low temperature cement bond, three or four holes about 19 mm (0.75 inch) in diameter were core drilled at the point between the hub insert and the matrix. Then ceramic "roll pins" were cemented into these holes. Since this latter approach resulted in no additional durability gain, the low temperature cement was abandoned.

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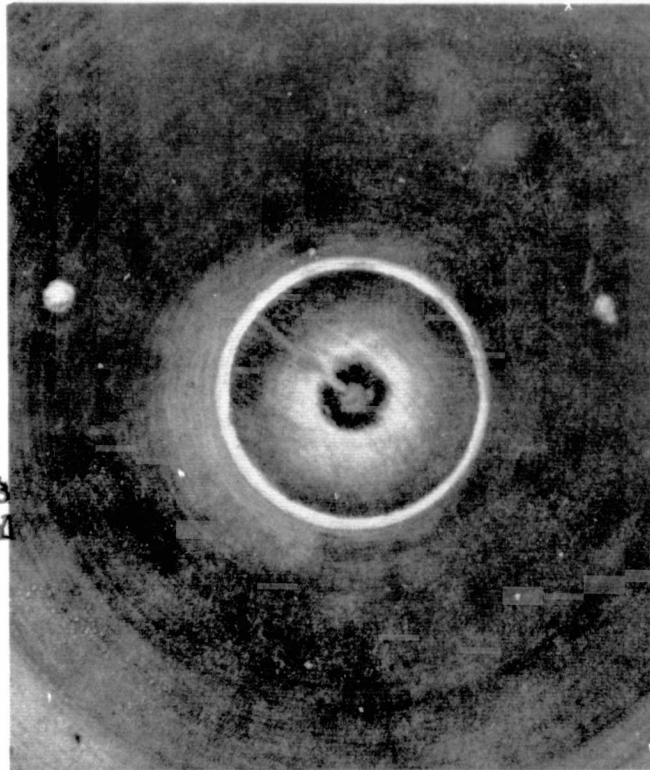


Figure I. B. 5. 3 Thin-Wall AS Core with Hub Repaired with Low Temperature Cement

It has been necessary to remove the ring gears and return the cores to the Supplier for installation of a new hub using a high temperature cement. This approach destroys the elastomer and the running history accumulated on it. A thick-wall core that has a new hub held in position with a high temperature cement is shown in Figure I. B. 5. 4.

In this arrangement the hub is cemented into a thin, 50 mm (.25 inch) wide, solid ceramic ring and this sub-assembly is then cemented into the matrix. The ceramic ring allows better control of temperature during the firing of the cement, and it also provides a better match of the thermal expansion characteristics of the insert-matrix bond area to the rest of the matrix.

All AS cores with repaired hubs now on test have been updated to this configuration, and all new cores received from Supplier A have been built with this design. As a result, six cores with this new hub configuration have been on durability test since late 1976, and there have been no failures. Two of these cores have accumulated over 2300 hours, so that results to date are encouraging. More test data, however, are needed before this problem can be considered solved.

As described in Section I. B. 3 the cores can be damaged when the hubs fail and/or during the subsequent repair. This fact is illustrated by comparing Figure I. B. 5. 1. and I. B. 5. 4 taken of core No. 43 before and after hub repair. Small radial cracks are seen in Figure I. B. 5. 1 after the initial hub failure. The additional processing and another high temperature firing which are required to recement a new hub in place have caused these cracks to grow significantly as shown in Figure I. B. 5. 4. As noted in Section I. B. 3 this damage, which occurred during the hub failure and subsequent repair, eventually caused the failure of core No. 34. New cores with the ceramic ring holding the hub insert in place do not show any distress in the hub region.

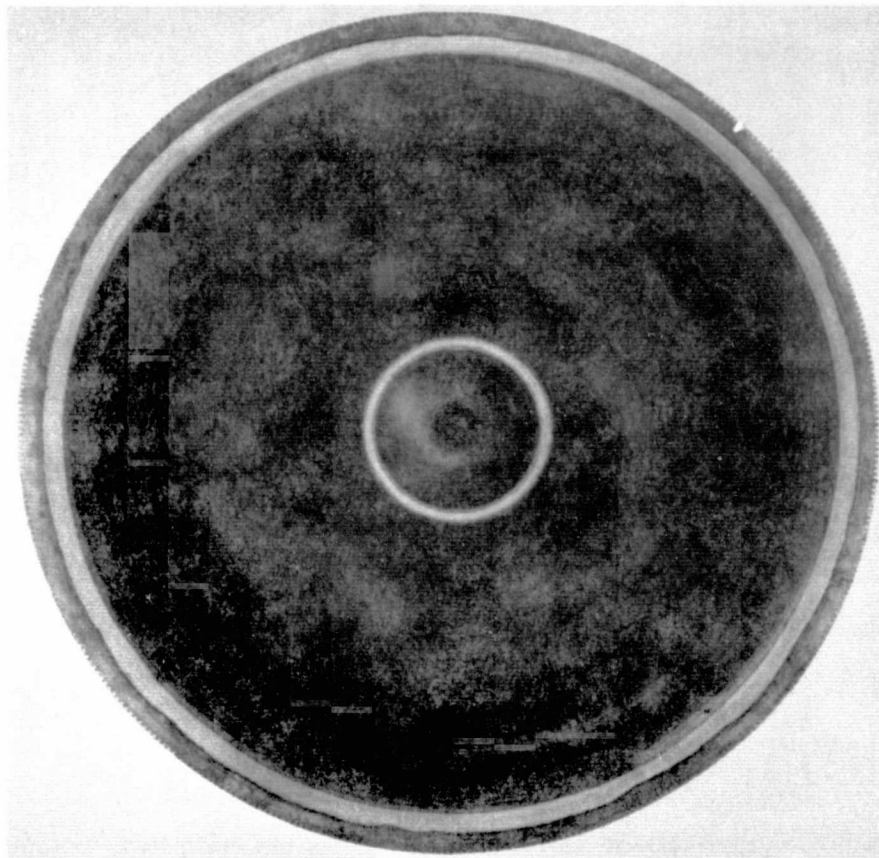


Figure I.B.5.4 Thick-Wall AS Core No. 43 with Hub Repaired with High Temperature Cement and Solid Ceramic Ring

Figure I.B.5.5 shows the hub cement failure that occurred in the MAS core described in the previous section. The hub in this core had been cemented in place with the same low temperature cement used with the AS core shown in Figure I.B.5.3. The cement used in the MAS core lasted 3752 hours before it failed. The better performance of this cement in the MAS core is attributed to the fact that the expansion of this material more nearly matches that of the cement, and this MAS material is stronger in the radial direction and better able to withstand any additional matrix-cement expansion mismatch. The hub in this core was recemented with improved low temperature cement and has accumulated another 1628 hours.

These hub failures are still not considered to be a serious, fundamental problem. Hubs utilizing a solid ceramic ring and high temperature cement have been fabricated into several AS cores and have shown good durability. Low temperature cement has shown reasonable durability in MAS cores, and with improved processing control may be adequate for this material. More engine test hours are needed to demonstrate that the present corrective action is adequate.

I.B.6 Matrix-Elastomer Bond separation

Reasonably good durability has been obtained with the elastomer bonded ring gear on the thick-wall AS core. Although separation did occur as low as 315 hours, the separations, for the most part, were traced to installation of the core in an engine with a worn pinion. The step wear pattern on these pinions introduced an axial load on the ring gear and elastomer which eventually caused a separation in the matrix-elastomer interface region.



Figure I.B.5.5 MAS Core with Hub Cement Failure

As reported in Section B of Reference 2, the elastomer shows little deterioration with running hours. This was confirmed recently when measurements of elastomer hardness (durameter) were taken from the core that had accumulated 6935 hours before it was irreparably damaged by a power turbine failure. The average durameter values radially and axially throughout the elastomer after 6935 hours of engine test is shown in Figure I.B.6.1. The approximate initial hardness after curing is about 70. This elastomer tends to soften slightly during initial exposure to temperature, followed by a gradual increase in hardness. The data from the 6935 hour regenerator shows that on the exposed cold and hot faces the elastomer hardness has exceeded its initial value. The data also show that it is still softer than its initial value in the inside of the elastomer where it was not exposed to an oxidizing atmosphere. This indicates good stiffness stability at operating temperatures for the present polymer after almost 7000 hours of test. Since hardening with time at temperature is not excessive, the elastic modulus should also be stable. Consequently, for a given ring gear deflection during engine operating conditions, the radial tensile load on the core should be unchanged.

The results obtained to date with the same elastomeric drive on the thin-wall AS core have not been as successful. Since the thin-wall matrix has a thinner cross section, it will be weaker and have less capability for carrying thermal and mechanical loads. Every thin-wall AS core, bonded with the same procedure used with the thick-wall cores, has had a separation in the elastomer-matrix bond area.

Some separations even occurred during bonding; an example of this is illustrated in Figure I.B.6.2. which shows the rim damage suffered by a thin-wall core in the bonding fixture. Every one of these thin-wall elastomer-matrix bond area separations occurred in the weaker matrix material. This is illustrated in Figure I.B.6.3 which shows the matrix securely attached to the elastomer. A more detailed examination shows that separation occurred in the second or third ceramic corrugation under the one attached to the elastomer.

The transient stress in a ceramic regenerator can be reduced by introducing a compressive load at the rim to counteract the operating tensile thermal stresses. This is accomplished in the Ford regenerator by bonding the ring gear to the core at an elevated temperature. The resulting contraction of the ring gear on cool down, after the elastomer has been allowed to cure at the higher temperature, provides a radial compressive preload on the regenerator. The amount of preload can be adjusted by changing the bonding temperature or by modifying the configuration of the elastomer.

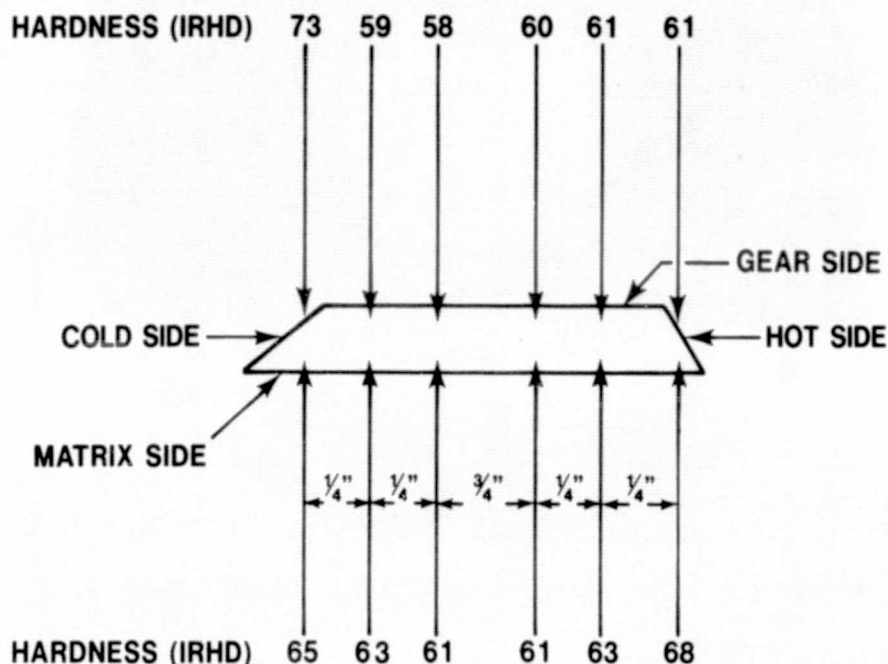


Figure I. B. 6.1 Hardness Readings in Elastomer After 6935 Hours of Engine Operation

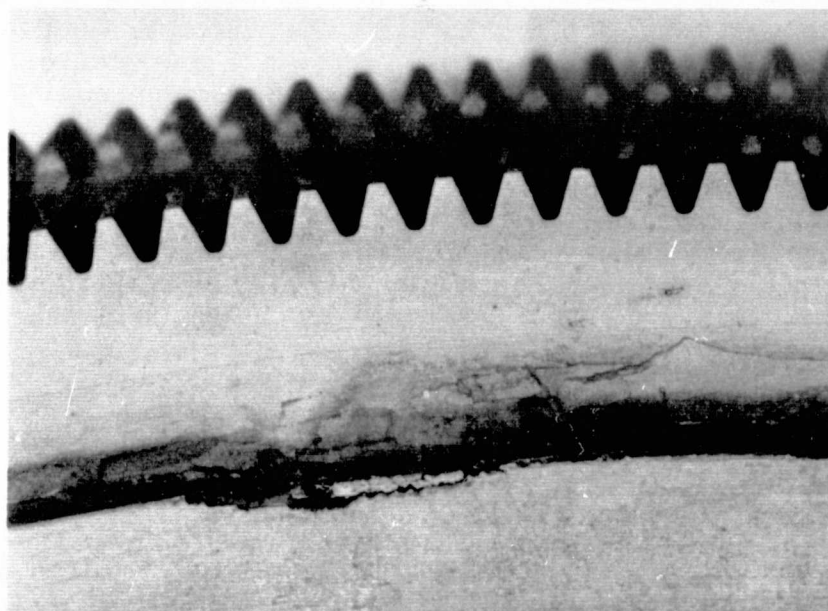


Figure I. B. 6.2 Circumferential Crack in the rim of a Thin-Wall AS Regenerator Initiated During Elastomer Bonding of the Ring Gear

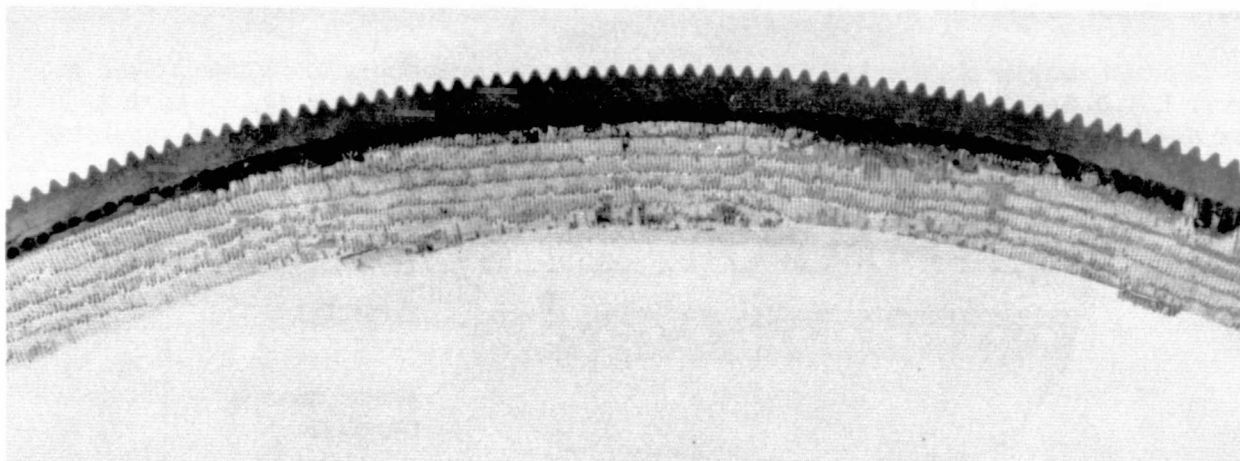


Figure I. B. 6.3 Elastomerically Bonded Ring Gear Which Has Separated from a Thin-Wall AS Core During Engine Operation

Three different levels of compressive preload have been applied to the thin-wall core with inconclusive results.

The zero compressive preload is accomplished by bonding the gear and curing the elastomer at room temperature. This results in a maximum operating radial tensile load being applied to the core by the more rapidly-expanding metal gear during engine transients. The single thin-wall AS core bonded with zero preload separated after 239 hours of engine test.

Three thin-wall cores were bonded at an intermediate temperature resulting in an intermediate compressive preload on the core at room temperature and a reduced radial tensile load on the core during transient operation. Two separated at low hours while the third was on the verge of separation at 1064 hours when the hub of this particular core came loose.

A maximum compressive preload is accomplished by bonding and curing the elastomer at its maximum possible temperature. This compressive preload neutralizes the greater growth of the ring gear during transients so that the elastomer is in a low stress state under these conditions. Two cores were bonded with a maximum compressive preload. One core developed so many cracks as it cooled down that it could not be used for engine test. The other core developed a crack but was placed on engine test. A separation in the elastomer-matrix bond region occurred in this core after 3885 hours.

Recent measurements of the thin-wall, AS matrix physical properties shows a large variation in strength. In addition, both the tensile and compressive radial strength of this matrix is very low, so that even moderate compressive or tensile loads during bonding or during engine operation will cause cracks and/or separation. This material data is presented in Section V. B. 1 of this report. The eventual solution to this problem is the development of a high-compliance, low modulus elastomer system to reduce the stress on the core during bonding and during engine operation. While the chemical development of a high-compliance elastomer has been started at Ford a significant amount of time may be required for this approach to become feasible.

Alternate approaches for increased compliance utilizing mechanical changes, but still retaining the same elastomer, are shown in Figure I. B. 6. 4. The upper configuration in the illustration incorporated over 300 holes in the elastomer and results in a 25% reduction in the radial modulus of the elastomer. The lower configuration

utilizes slots to reduced the radial modulus by almost 95%. Both are discussed in greater detail in Section V.B.3.

The test results obtained to date on these various approaches are summarized in Figure I.B.6.5. The cores shown on the left hand side of the figure are those that were described earlier and were bonded at three different levels of pre-load, utilizing

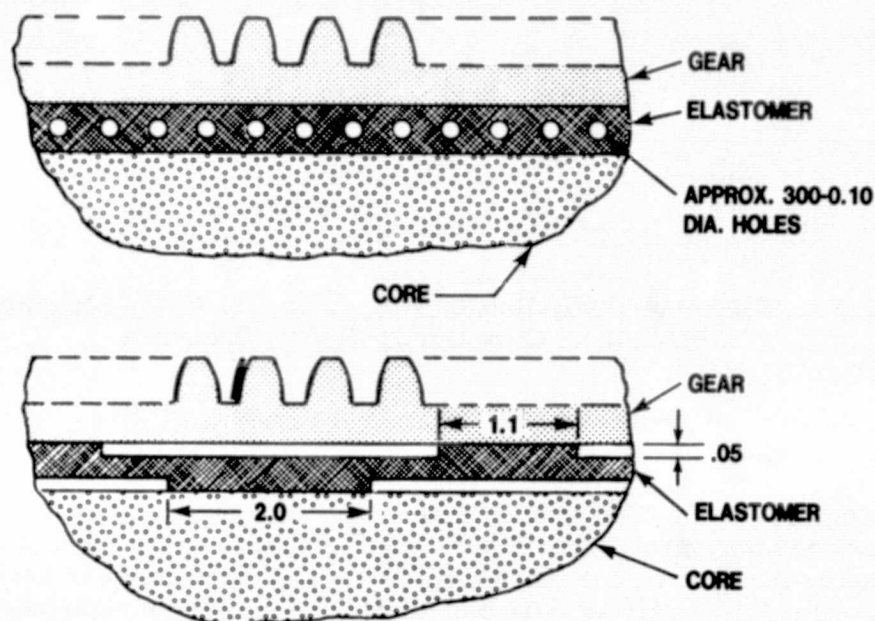


Figure I.B.6.4 Compliant Elastomer Schemes

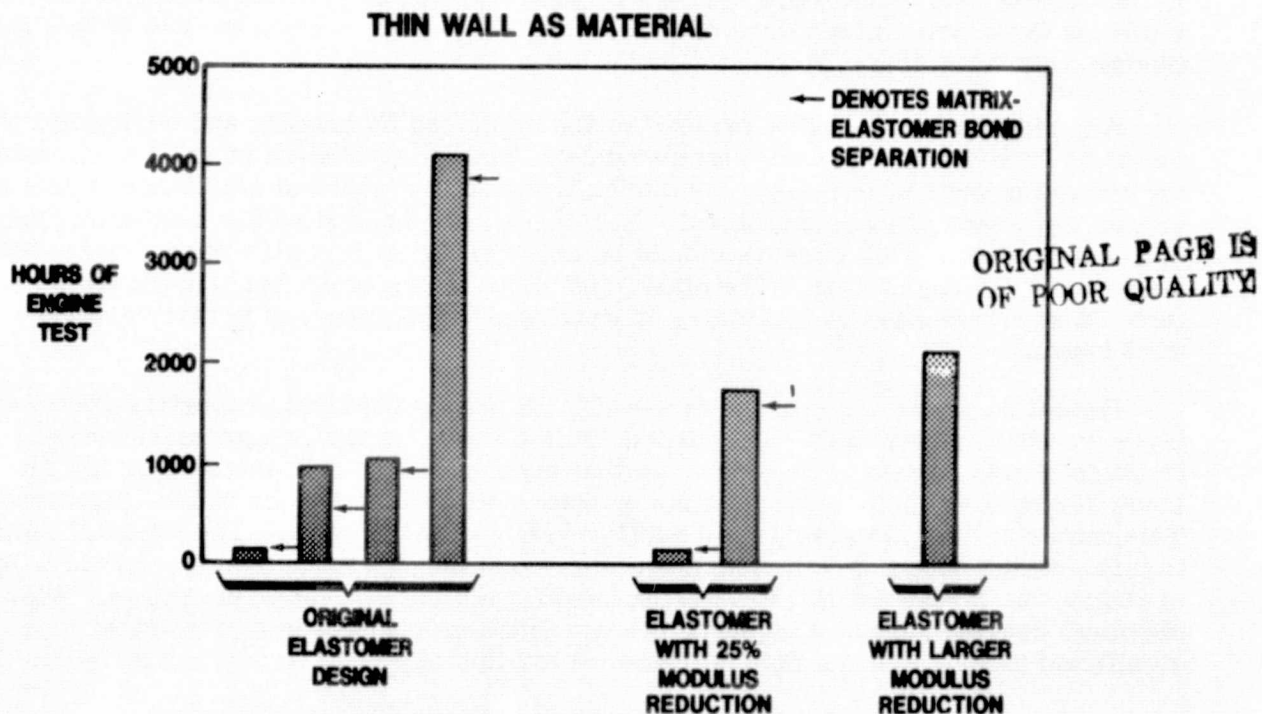


Figure I.B.6.5 Test Results on Thin-Wall AS Regenerators Utilizing Different Elastomer Bonding Approaches

conventional bonding techniques. Separation ranged between 239 and 3885 hours. The middle group is the elastomer containing the 300+ holes and having a modulus reduction of 25%. One early hour separation suggests that this approach is marginal and a greater reduction in modulus is required. The slot configuration with a 95% reduction in modulus is shown on the right hand side. This core has accomplished 2314 hours with the elastomer-matrix bond area still intact.

During the next report period additional low-modulus, high-compliance elastomer designs will be placed on engine durability test. Limited data from the core with slots in the elastomer, suggests that this approach may be successful with thin-wall AS cores.

I.B.7 Drive and Support System

Until the end of 1973, the Ford 707 regenerator drive and support system consisted of cylindrical solid ceramic pins cemented into the rim of the core. Spring clips provided an attachment between the ring gear and the solid ceramic pins in the rim. The core was mounted at the center through a solid ceramic center hub. This drive and support system is described in detail in Sections J and K of Reference 1.

In 1974, the design and development of a three point support system was initiated to facilitate the development of new chemically resistant ceramic materials. With this drive and support system, parallel development efforts for solid ceramic and cement technology were eliminated. Consequently, the present three point support system contains a regenerator matrix which does not contain solid ceramic pins or a solid hub. The ring gear, which is attached to the rim with an elastomer, is supported at three points (Figure I. B. 7. 1.). In addition to the main design impetus of maximizing the effort to develop new ceramic matrix materials, the elastomeric, rim-support system has the following advantages when compared to the earlier spring clip, hub-support system:

1. Reduces the possibility of cracks initiated by mechanical, thermal, or chemical stress from propagating to a center hub, and/or pins which ultimately separate from the matrix causing ring gear disengagement and engine shutdown.
2. Eliminates the need for a costly second firing of the matrix, which is required to cement the solid ceramic pins and hub to the core.
3. Permits the effective use of a continuous rim core with stress relief slots, which will be discussed in detail in Section V.B.2.

The present three-point system design (Figure I.B.7.1) has eliminated the bearing, ring gear and matrix problem areas which were associated with the first two design attempts. The early design configurations are described in detail in Section B of Reference 1.

Figure I. B. 7. 2. illustrates the force diagram of the present design. Since the major reaction force is at the fixed roller location, a yoke with two rollers is utilized (Figure I.B. 7. 3) at this point. The lightly loaded spring roller and pinion locations are shown on Figures I.B. 7.4 and I.B. 7.5, respectively. Except for the pinion location the roller assemblies contain an outer race support ring (Figure I.B.7.6) for the ball bearing. This provides additional rigidity, reduced bearing speeds, and lower contact stresses in the bearing.

Since the primary objective for operating these engines is to accumulate durability time on candidate regenerator materials and designs, the bearings are inspected every

350 to 400 hours. Consequently, any difficulties in the drive system can be alleviated before damaging a high-hour regenerator matrix. As a precautionary measure, the bearings are re-greased after each inspection.

At the present time ten engines have been on test with the current three-point support system. No major difficulties have been encountered after 315, 350, 630, 1345, 1565, 1980, 2700, 2850, 4350 and 5825 hours for a total of 21910 operating hours.

I. C. PROBLEM AREAS

Two secondary problem areas exist and they are: failures of the cement bonding the hub insert to the matrix and separation at the elastomer-matrix interface in the thin-wall AS regenerator. The first problem is discussed in Section I. B. 5 and the second is discussed in Section I. B. 6. Corrective action consists of a high temperature cement-ceramic ring configuration for the first problem and high-compliance elastomer design for the second problem. Hardware incorporating these changes has now been started on engine test.

I. D. FUTURE PLANS

During the next quarter engine testing will continue on both the thick and thin-wall AS cores and the MAS core. Core dishing as well as the durability record of the new hub configuration and high compliance elastomer systems will be carefully monitored. More thin-wall AS cores with the high compliance elastomer system, as well as more MAS cores, will be placed on engine test in the next quarter.

I. E. TASK SUMMARY

Approximately 19,600 hours of engine durability test (39,200 core hours) were completed from October 1, 1976 to September 30, 1977 on cores made from chemically resistant materials and mounted on rim support and drive system.

Turbine engine durability tests on aluminum silicate regenerator cores tend to confirm the results of the accelerated chemical attack engine tests, which showed that this material was relatively impervious to chemical attack. Eight cores of this material have each accumulated over 4000 hours of engine test at 800°C (1472°F) and one core has attained 6935 hours of engine test with a minimal amount of dishing, which is an indication that no significant chemical attack has occurred.

A single high expansion, rim-mounted MAS core accumulated 5389 hours at 800°C (1472°F). The thermal cracks formed in the rim of the matrix during the first 200 hours are stable and will not propagate unless the operating temperature is raised above 800°C (1472°F).

One of Supplier A's thin-wall AS cores has now accumulated 4268 hours of engine test. This weaker construction appears to be able to withstand the seal shoe rubbing forces without serious wearing or chipping. Elastomeric bonding of the ring gear to this material requires additional development, since separations in the elastomer-matrix bond region have occurred on all thin-wall cores bonded using the conventional technique. Utilization of a high-compliance elastomer system shows promise of solving this problem, and one such configuration has now accumulated 2314 hours of engine test without distress.

The cement holding the hub inserts in place failed in five out of the eighteen AS cores that were engine tested. Hub repair with a low temperature cement has proven to be inadequate and this approach has been abandoned for AS material. A hub

configuration which utilizes a high temperature cement and a solid ceramic ring shows potential. There have been no failures in the six cores of this design that are now on test, and two such cores have each accumulated over 2300 hours.

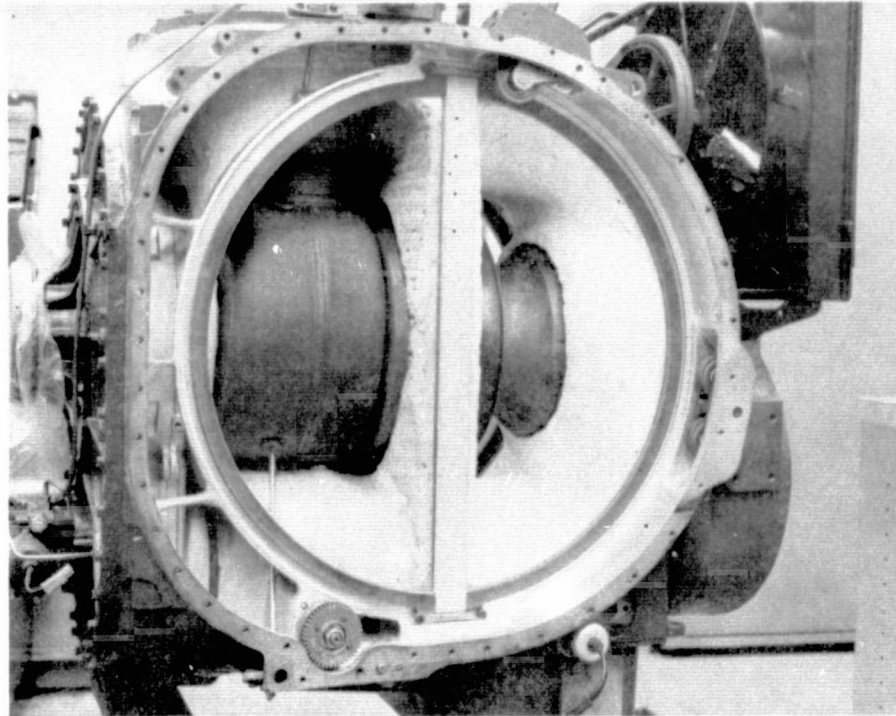
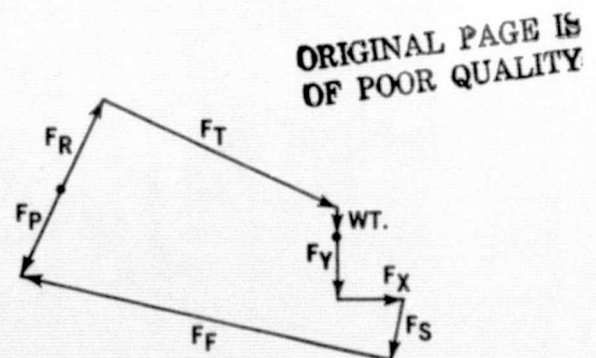
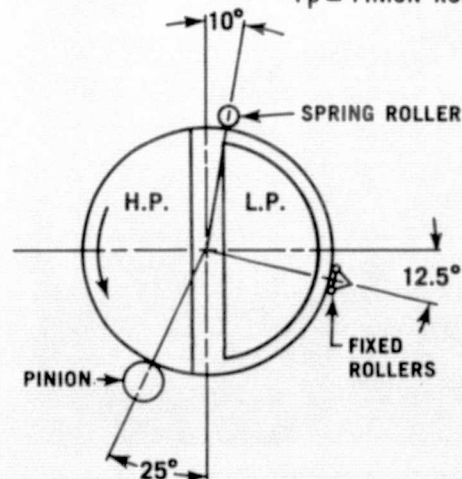


Figure I.B.7.1 Photograph of Ford 707 Turbine Engine Housing Showing Modifications Required to Incorporate the Present Rim Support System.

TORQUE = 680 N·m (500 LB.-FT.)
 WT. = 222 N (50 LB.)
 F_Y = VERTICAL SEAL REACTION FORCE = 445 N (100 LB.)
 F_X = HORIZONTAL SEAL REACTION FORCE = 445 N (100 LB.)
 F_R = GEAR SEPARATING FORCE = 666 N (150 LB.)
 F_T = GEAR DRIVING FORCE = 1870 N (430 LB.)
 F_F = TOTAL FIXED ROLLER REACTION FORCE = 2710 N (610 LB.)
 F_S = SPRING ROLLER FORCE = 222 N (50 LB.)
 F_P = PINION ROLLER FORCE = 445 N (100 LB.)



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Figure I.B.7.2 Regenerator Force Resolution for the Modified Rim Support System at 680 Nm (500 ft-lbs) Torque Condition

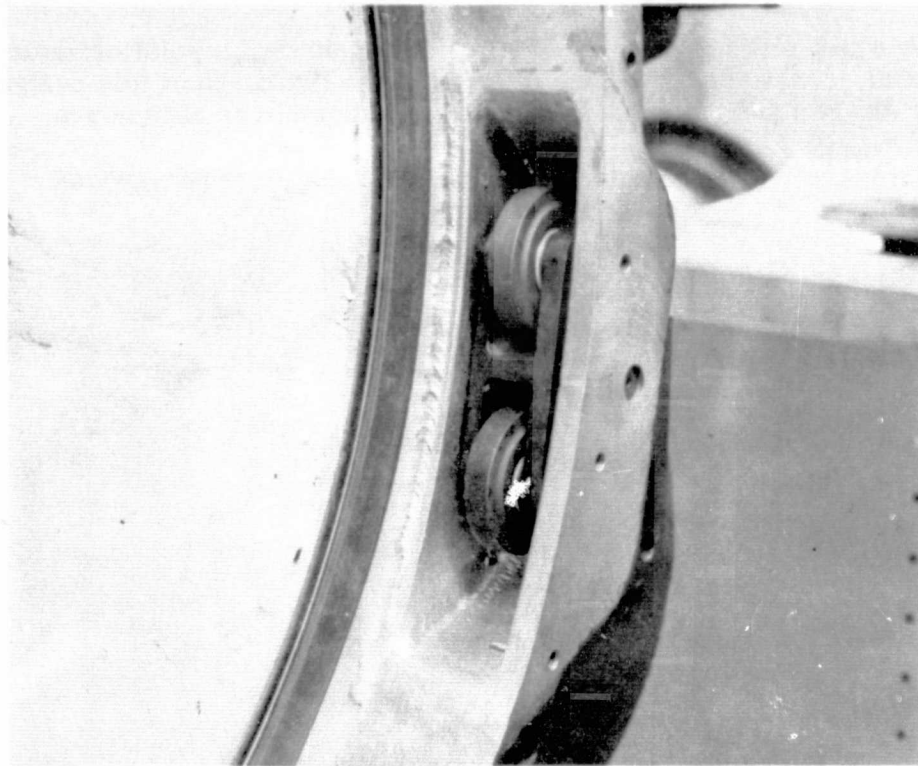


Figure I. B. 7.3 Photograph of Ford 707 Turbine Engine Housing Showing
Location of Fixed-Support Rollers

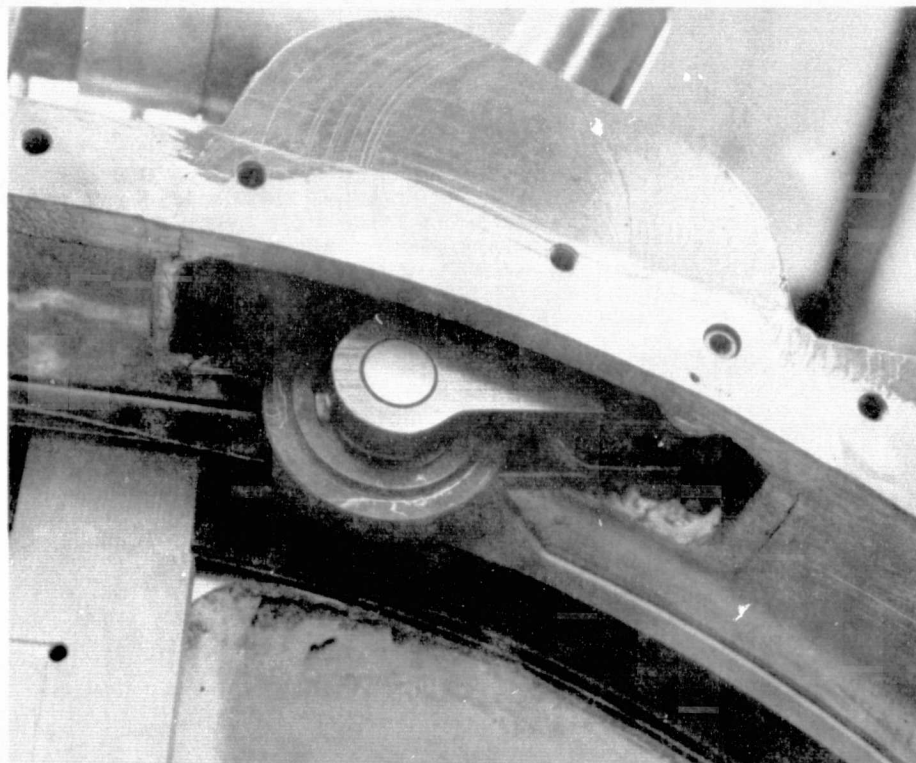


Figure I. B. 7.4 Photograph of Ford 707 Turbine Engine Housing Showing
Location of Spring-Loaded Roller

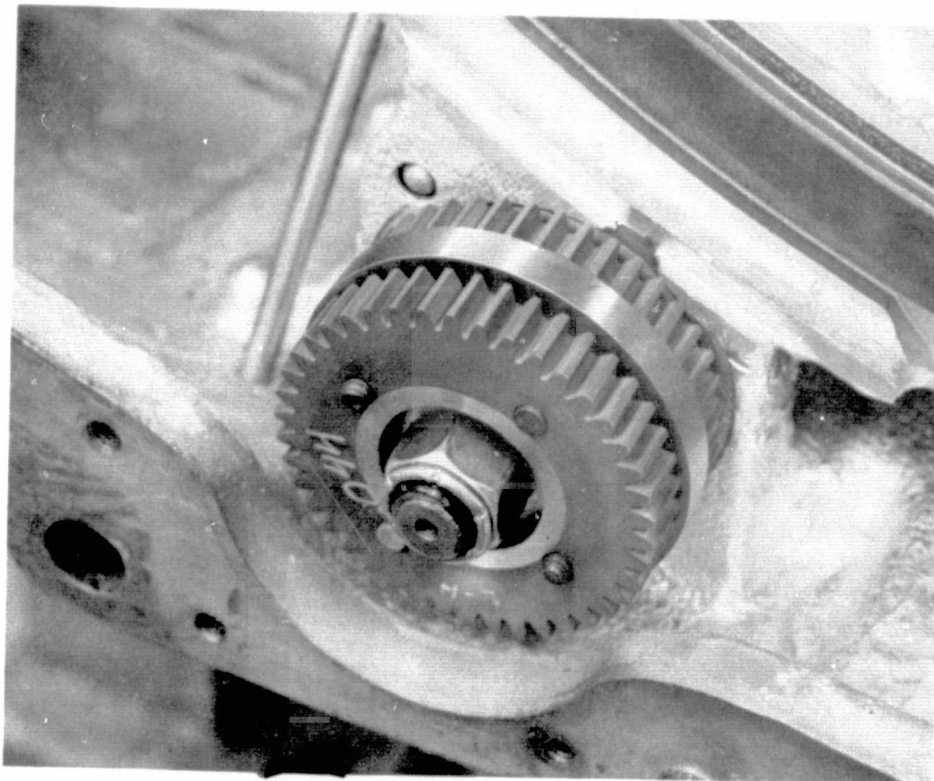
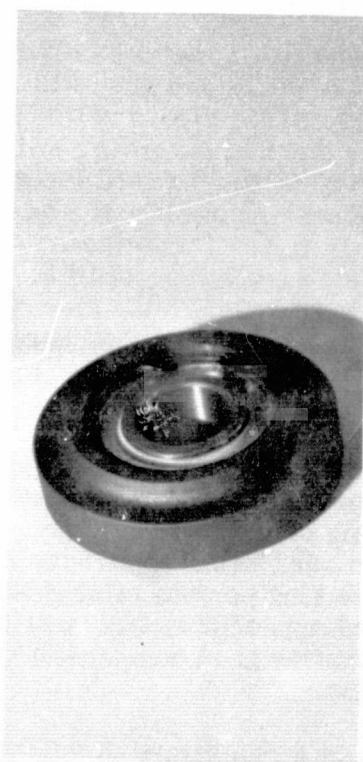


Figure I. B. 7. 5 Photograph of Ford 707 Turbine Engine Housing Showing Location of Drive Pinion Assembly



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Figure I. B. 7. 6 Photograph Showing Ball Bearing, Outer Race Support Ring and Snap Ring

TASK II CORE DURABILITY TESTING AT 1000°C (1832°F)

II. A. INTRODUCTION

A regenerator inlet temperature of 1000°C (1832°F) is a short-term goal that is needed for use in the current Stirling and ceramic gas turbine engines now under development. As a result of this projected need, in the fourth quarter of 1975, a special 707 turbine engine was assembled with high temperature regenerator seals and additional compressor discharge air cooling of the main frame bulkhead. Previous testing had shown these changes were needed to prevent seal coating failures and thermal distortion of the main frame. This engine contained the rim-support regenerator core mounting system. A thick-wall aluminum silicate regenerator core was installed on the left hand side and a thick-wall core of Supplier A's CPD-6 material was installed on the right hand side. Both cores were rim mounted with elastomer bonded ring gears and stress relieved rims. Throughout the test the engine has been operated at an average regenerator inlet temperature of 982°C (1800°F) with excursions of 30°C (52°F) above and below this value being permitted. These regenerator inlet temperatures are obtained by operating the engine at higher-than-design turbine inlet temperatures at 60 to 65% gasifier spool speed and low power turbine speeds. It was possible by operating at these conditions with turbine inlet temperatures of 1065-1080°C (1950-1975°F) to expose the regenerator to inlet temperatures up to 1020°C (1850°F). Since this elevated test program was initiated, it has been necessary to completely rebuild the engine twice. On several other occasions it has been necessary to replace nozzles, turbine rotors, and sheet metal ducting as these parts distorted, oxidized, or crept under thermal loads.

The objective of Task II of the DOE/NASA Ceramic Regenerator Program is to accumulate 2000 engine hours per year on experimental regenerator systems at an inlet temperature of 1000°C (1832°F), so that 3500 hours at this temperature can be accumulated on one or more cores.

II B. STATUS

Early in this test, thermal rim cracks were detected in the rim of the CPD-6 core. These cracks which were first observed at 418 hours are shown in Figure II. B. 1. Radial cracks emanate from the base of the stress relief slots. These cracks are believed to be caused by high rim thermal stresses, and not totally unexpected in light of the fact that the rim safety factor for this design is just above unity. The thermal stress safety factors for aluminum silicate and CPD-6 material at these temperatures are listed in Table II. B. 1. These values suggest that the CPD-6 material will be marginal at these elevated temperatures. Providing the material is thermally stable, the aluminum silicate regenerator should have no problems with thermal stresses at 1000°C (1832°F). To date, the AS core has accumulated 3510 hours at an average inlet temperature of 982°C (1800°F) and it shows no evidence of thermal or chemical distress.

The cracks in the CPD-6 material did not grow radially in the next 1400 hours. Figure II. B. 2 shows the same cracks after 1710 hours. The width of the cracks appear to have increased, but this is merely fretting or crumbling of the edge of the crack caused by the rubbing action of the crossarm seal shoe. This edge fretting has been observed previously in cracks that survive long periods of engine exposure. The fact that the length of the crack has not changed appreciably indicates that the crack may have propagated to a region where the matrix is in compression. The crack then appears to have stabilized at this position.

After 2307 hours of operation at an average inlet temperature of 982°C (1800°F), the CPD-6 core failed, and a photograph of this failure is shown in Figure II. B. 3.

The failure mode is typical of a chemical attack failure with mid-radius cracks formed on both faces. This material had shown only marginal improvement in chemical attack resistance when compared to LAS in engine tests that were conducted at 800°C (1472°F) (Reference 2). It had also shown only a small improvement over LAS in laboratory tests at 1000°C (1832°F) with sodium present (Reference 2). Eventual failure due to chemical attack, therefore, was expected in these high temperature engine tests. This material has now been dropped from the engine test program.

The CPD-6 core has been replaced with a thin-wall AS core and the test is continuing. The present status is summarized in Table II.B.2 and shows this thin-wall core has now accumulated 1182 hours at an average inlet temperature of 982°C (1800°F). A failure at the elastomer bond-matrix interface occurred in this thin-wall core after 271 hours. The failure is typical of thin-wall AS cores bonded with the original process. The core has been rebonded and returned to test. This failure mode and the corrective action are described in Section I.B.6.

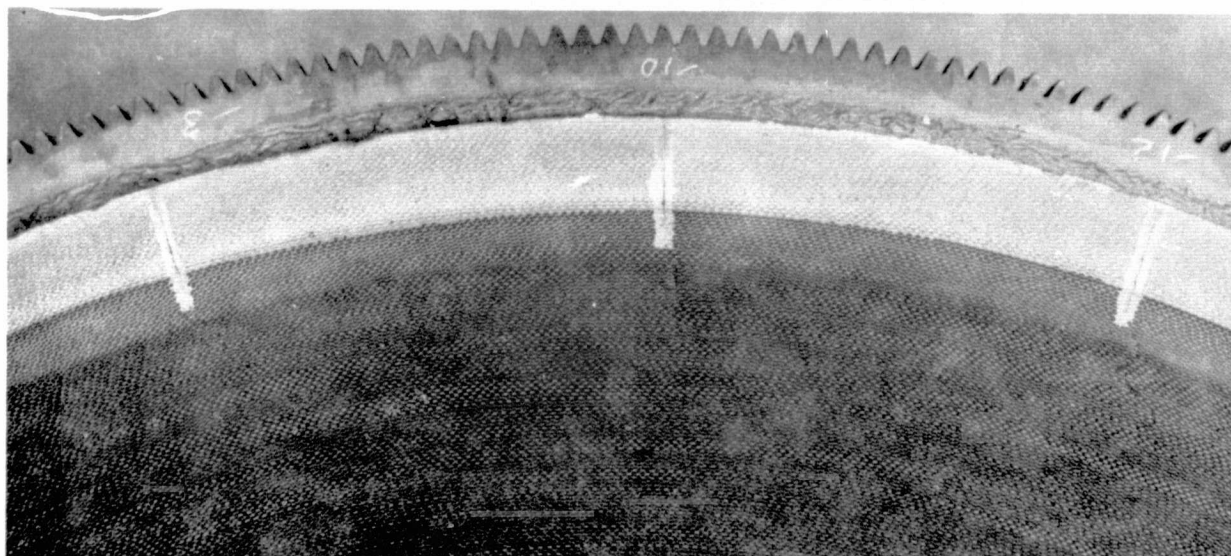


Figure II.B.1 Supplier A's CPD-6 Regenerator After 418 Hours at 982°C (1800°F)

RIM CONFIGURATION	MATERIAL	THERMAL STRESS SAFETY FACTOR
STRESS RELIEF, CONTINUOUS RIM	CPD6	1.5
STRESS RELIEF, CONTINUOUS RIM	ALUMINUM SILICATE	7.5

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Table II.B.1 Safety Factor Comparison for Two Different Ceramic Materials at 982°C (1800°F)

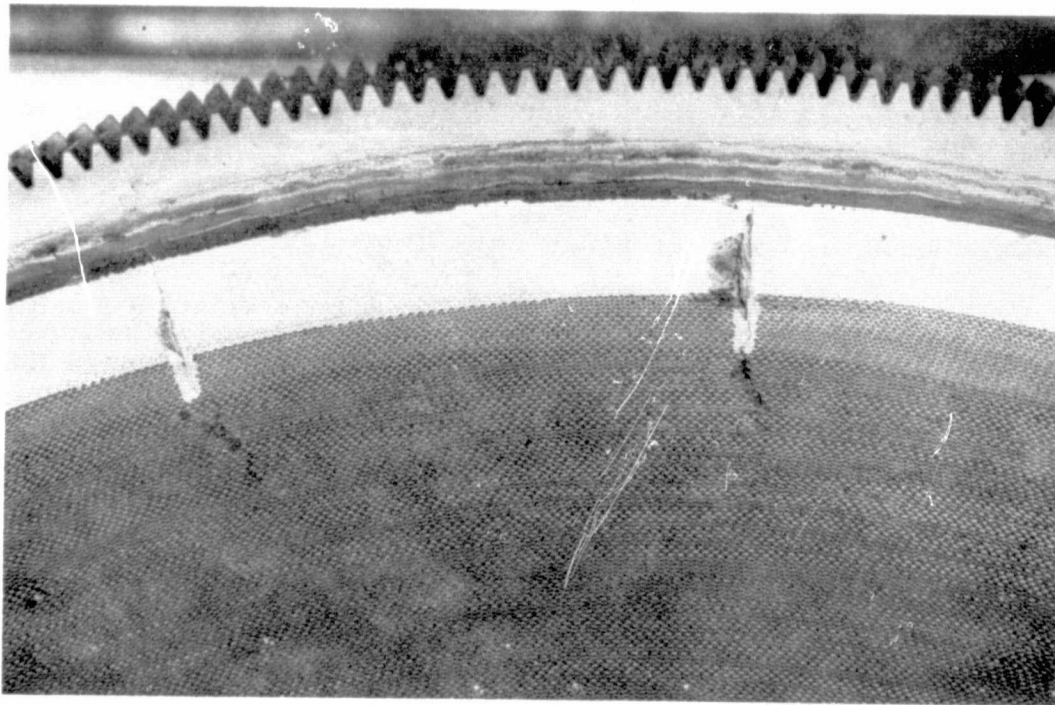


Figure II.B.2 Supplier A's CPD-6 Regenerator After 1710 Hours
at 982°C (1800°F)

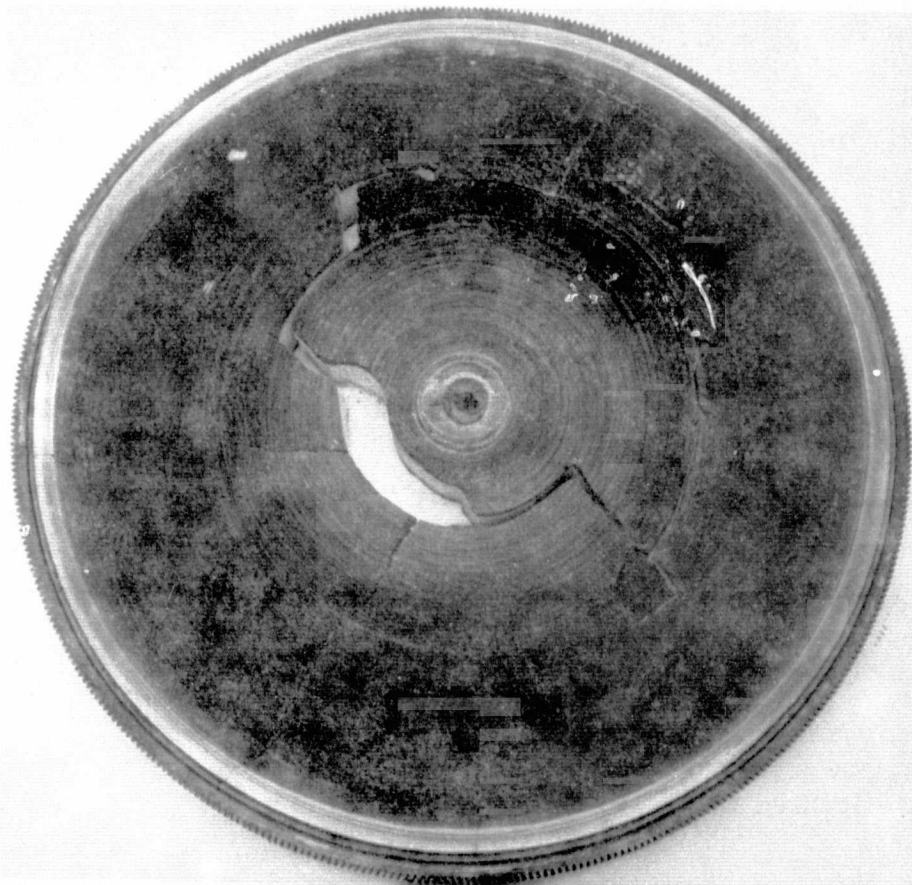


Figure II.B.3 Failed CPD-6 Core After 2307 Hours at 982°C (1800°F)

MATERIAL	HOURS	STATUS
THICK WALL AS	3510	NO DISTRESS
CPD6	2307	CHEMICAL ATTACK FAILURE
THIN WALL AS	1182	ELASTOMER- MATRIX BOND FAILURE

Table II.B.2 Summary of Engine Tests at 982°C (1800°F)

II. C. PROBLEM AREAS

On two occasions during this report period, it was found that the main frame had distorted to the point where the regenerator cores could be jeopardized. On both occasions the engine was completely rebuilt with new components (except cores) and a new main frame housing. Since specially machined housings are required for this test, a certain amount of delay was encountered in the test program while the engines were being built. To avoid a third occurrence of this kind of delay, an additional housing is being machined. If the present housing distorts because of the high operating temperature, it will be rapidly exchanged with the spare one, thereby reducing the amount of test time lost.

II. D. FUTURE PLANS

Engine durability testing of the thick and thin-wall AS cores will be continued at 1000°C (1832°F).

II. E. TASK SUMMARY

About 3510 hours of engine test at an average regenerator inlet temperature of 982°C (1800°F) have been accumulated on a thick-wall aluminum silicate core. The aluminum silicate core shows no signs of thermal or chemical attack damage after this exposure.

Supplier A's CPD-6 core failed after 2307 hours of exposure to the same test conditions. Failure was due to chemical attack and this material has now been phased out of the engine test program.

A thin-wall AS core has replaced the failed CPD-6 core, and it has now accumulated 1182 hours at an average inlet temperature of 982°C (1800°F).

TASK III. MATERIAL SCREENING TESTS

III.A. INTRODUCTION

The material screening tests represent a three-fold testing program which has been carried out at Ford Motor Company and which forms the basis for the evaluation and subsequent selection of ceramic regenerator materials which may have the potential for long-term service in the gas turbine engine. The materials screening program consists of tests at three levels: the laboratory, in situ engine tests as core inserts, and finally as a full sized regenerator core. This testing scheme is envisioned as a progressive materials selection process. Materials will first be tested on a laboratory scale using tests that are designed to subject the materials samples to conditions similar to those existing at the hot and cold face of the regenerator. Materials deemed potentially interesting, based on the results of these laboratory tests, will next be tested as small inserts in a host core running in an engine operating in an accelerated corrosion mode (ingested sodium chloride), with a regenerator inlet temperature of 800°C (1472°F). Materials which remain stable under these hostile conditions will be put into service as a full size regenerator core, and the test engine will again be operated at 800°C (1472°F) regenerator inlet temperature under accelerated corrosion conditions. Therefore, the emphasis in this testing program is the logical, progressive testing of materials in such a manner as to yield material candidates that exhibit chemical and physical stability under engine operating conditions. Any materials surviving the series of three, progressive tests should be prime candidates for durable regenerative heat exchangers for gas turbine engines.

The initial portion of the materials screening test is the laboratory testing. The laboratory testing program consists of tests designed to evaluate a candidate material's resistance to attack by sodium and sulfur. Specifically, the tests are designed to promote sodium ion exchange and sulfuric acid leaching of the materials as may be experienced under engine operating conditions at the hot and cold face, respectively, of the regenerator core.

The cold face chemical attack test treats the materials to a 2 hour leaching in 1% sulfuric acid at ambient temperature. The specimens are then heated to 315°C (600°F), which approximates the turbine regenerator core cold face operating temperature, to promote H^+ -for-core-cation exchange. The degree of stability of the sample is discerned by measuring changes in sample length as well as changes in sample thermal expansion behavior between room temperature and 800°C (1472°F) as a result of a series of such chemical treatments.

The hot face chemical attack test provides a source of sodium ions for exchange with a host lattice cation by soaking the material in a 3.5% sodium chloride solution. The treated specimen is air dried for 2 hours at 200°C (392°F), fired at 800°C (1472°F), and stability (change in length) measurements are periodically carried out. The change in the material's thermal expansion behaviour between room temperature and 800°C (1472°F) is also determined, as a significant change in this material characteristic is an indication of material instability.

Materials successfully surviving the laboratory tests are then moved into the second of the materials screening tests which is the accelerated salt corrosion testing of regenerator matrix inserts. In this test, cylindrical matrix test inserts are cemented into a host core and run in a Ford 707 gas turbine test engine operating under conditions of solid salt ingestion and a regenerator inlet temperature of 800°C (1472°F). Common road salt, ground to-150 mesh, is introduced, in a controlled manner, into the engine inlet. Samples are periodically (every 40 test hours) taken from the core inserts, and a chemical analysis indicates the amount of sodium

taken up by the crystalline lattice of the material being tested. The test proceeds for 120 total engine hours and the change in the thermal expansion behavior of the material between room temperature and 800°C (1472°F) is determined as another indication of material corrosion attack.

Materials exhibiting the best resistance to the preceding accelerated chemical attack testing will then be fabricated into full-sized cores (depending on specific vendor fabrication technology) and tested under similar accelerated corrosion conditions. Again, periodic chemical analysis will serve to monitor sodium ion exchange with core cation species. Additionally, visual inspections and core dishing (rim to hub deflection) measurements will be carried out at each 50 test hour sampling point. This test will continue up to 500 engine hours, providing core failure does not terminate the testing procedure. Finally, the core's thermal expansion between room temperature and 800°C (1472°F) will be determined. These data, coupled with the chemical analyses and the core dishing measurements, will serve to specifically delineate the material's resistance or susceptibility to accelerated chemical attack at 800°C (1472°F).

III. B. STATUS

III. B. 1 Laboratory Tests

A portion of the materials selected for testing this contract year have been received, have been prepared for testing, and are currently on test. Other materials are expected to be supplied soon. A third class of materials exists: those which have been promised but for which no firm delivery date has been verified. Negotiations, intended to encourage delivery, continue with specific material manufacturers.

Duplicate specimens of the materials placed on test in the laboratory were all prepared in a similar manner. Standard laboratory specimens, nominally 25.4 mm x 25.4 mm x 76.2 mm (1 inch x 1 inch x 3 inch) rectangular parallelepipeds were cut from bulk matrix honeycomb with the major dimension in the axial direction. The axial faces were ground to be flat and parallel to each other to within 0.025 mm (0.001 inch). The samples were then degreased by a five-minute ultrasonic treatment in ACS acetone. A five minute ultrasonic cleaning in distilled water was then carried out, and the excess water was blown out of the honeycomb samples using filtered compressed air. The samples were dried at 200°C (392°F) and fired to 500°C (932°F) to complete the cleaning procedure. Initial length measurements of the cleaned samples were determined using a Sheffield Visual Comparator with 5000:1 amplification. The lengths are measured to the nearest 2.5×10^{-5} mm (one millionth of an inch) and the measurement is reported to be accurate to $\pm 1.3 \times 10^{-4}$ mm (± 5 millionths of an inch).

Figures III. B. 1. 1 and III. B. 1. 2 graphically display some early test results. The difference in ordinate and abscissa scales between figures should be noted. These data point out the obstinate character of nature: those materials exhibiting the best cold face stability are among the least stable materials under hot face conditions. However, it appears, based on admittedly preliminary hot face test results, that MAS and aluminous silicate (AS) materials are promising. Data of this type (cumulative change) are best interpreted for trends, as an error in the initial length measurement or in the determination of the first experimental point can introduce a fixed bias that may be misleading. In that perspective, the longer time data still to be gleaned from the hot face test may prove to be more significant.

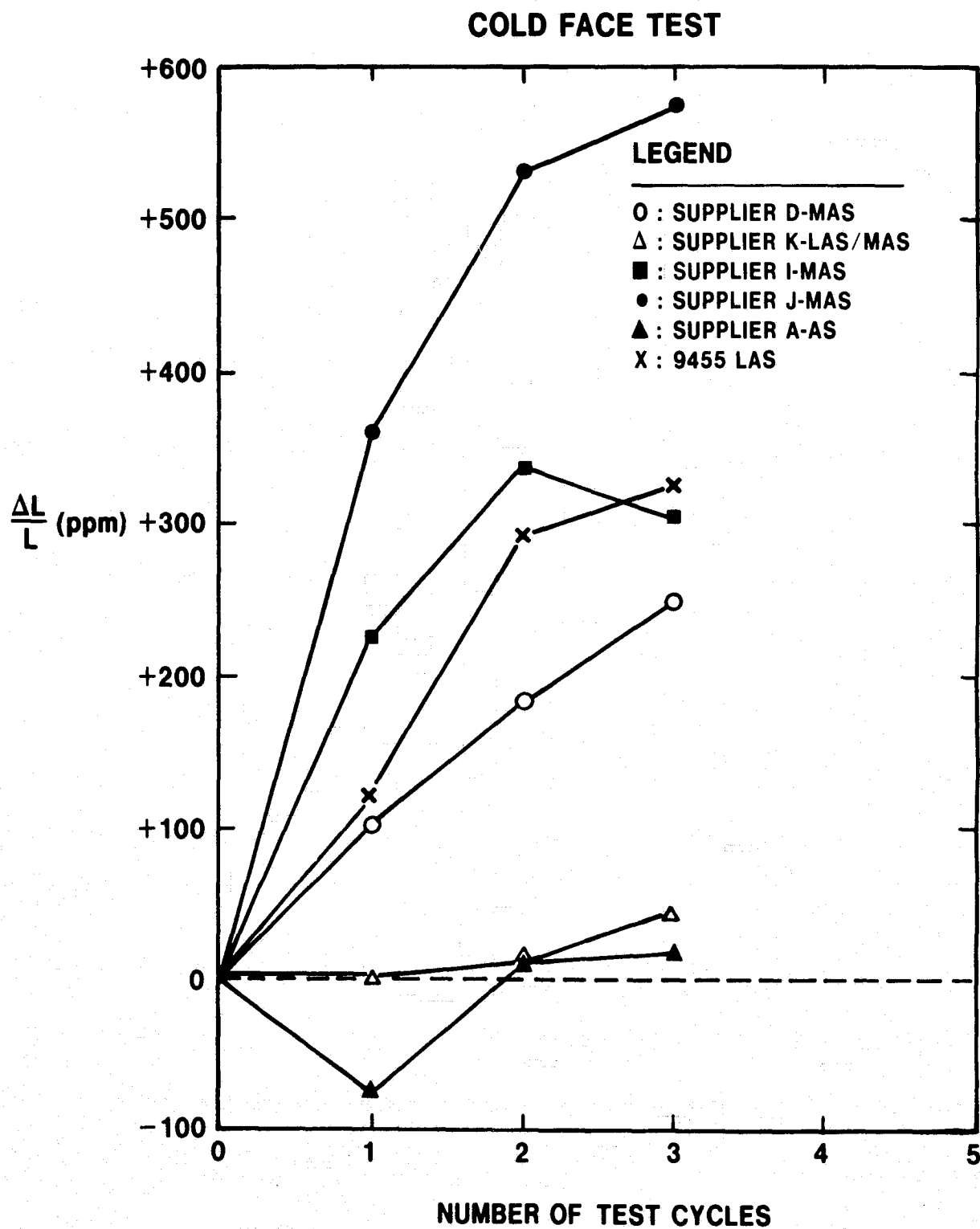


Figure III.B.1.1 Physical Stability of Various Candidate Regenerator Materials Under Cold Face Test Conditions.

HOT FACE TEST

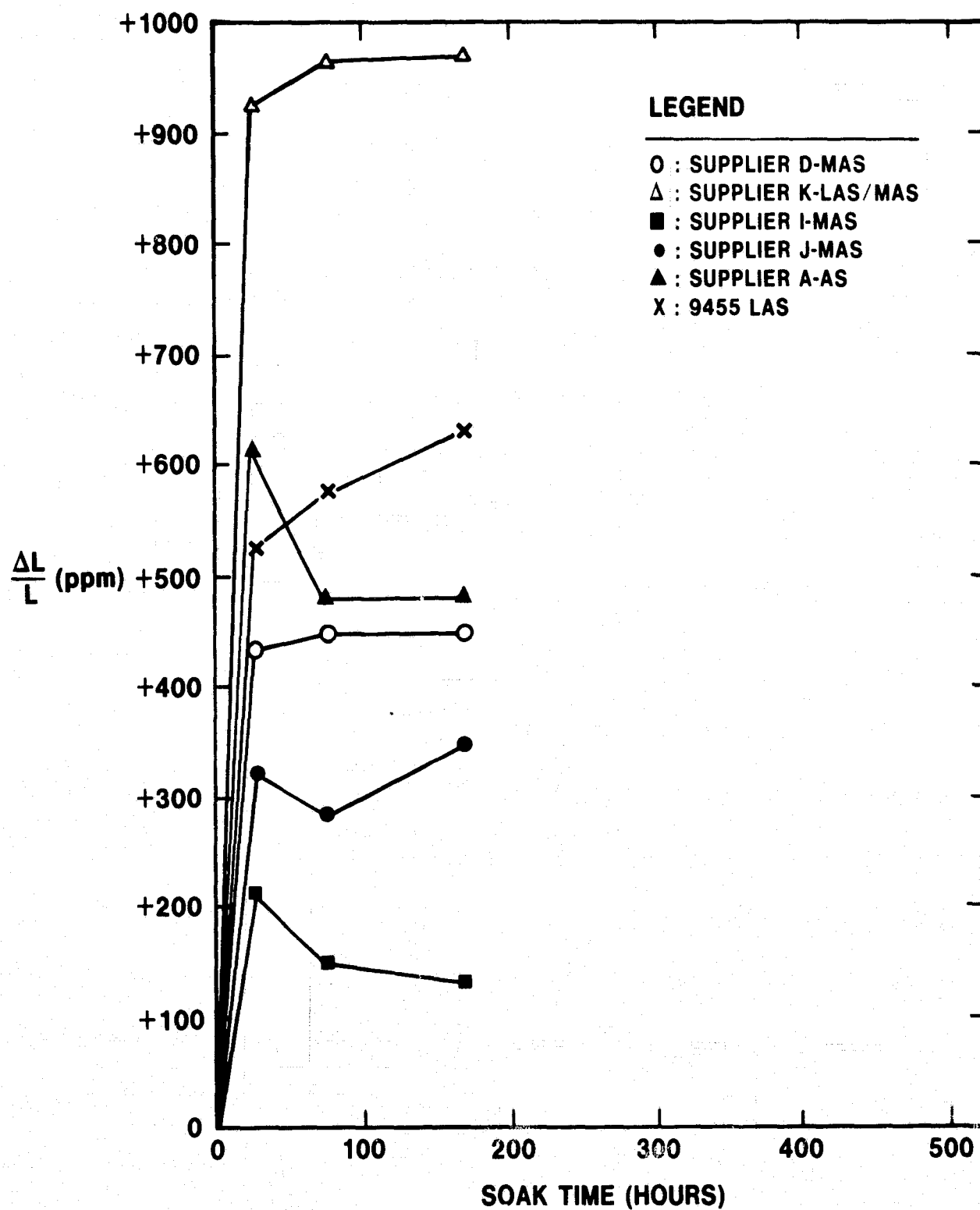
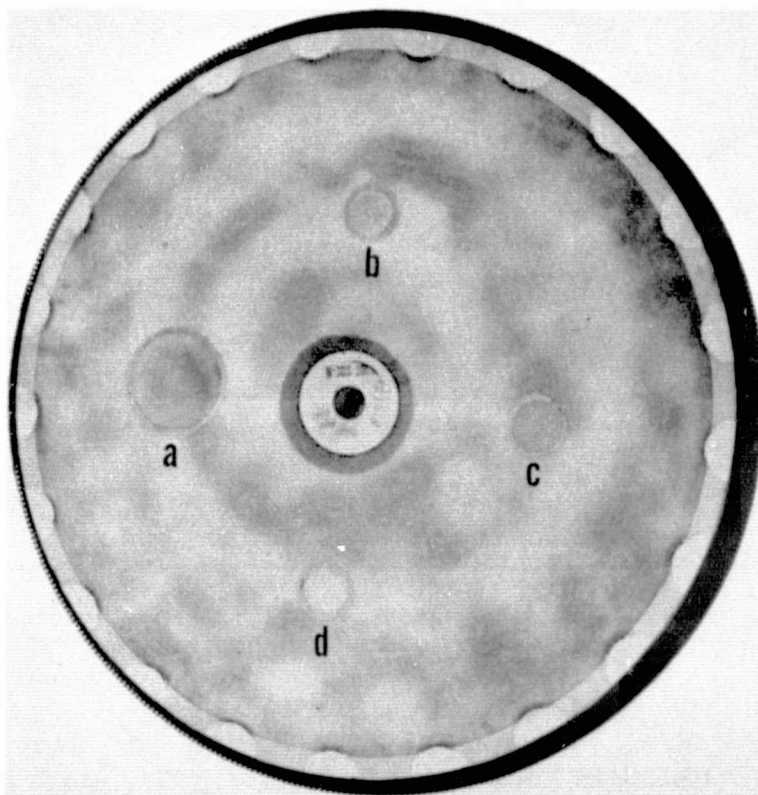


Figure III.B.1.2 Physical Stability of Various Candidate Regenerator Materials Under Hot Face Test Conditions.

III. B. 2 Accelerated Corrosion Testing — Matrix Inserts

Because of the time anticipated to procure all of the materials of interest for testing at the laboratory level, a decision was made to implement this portion of the testing program before all of the laboratory results were in hand. Figure III.B.2.1 displays the first accelerated test core assembly consisting of an LAS host core with the following inserts: (a) Supplier K-LAS/MAS, (b) Supplier J-MAS, (c) Supplier I-MAS, and (d) Supplier E-MAS. The large core insert is 102 mm (4 inches) in diameter, while the smaller inserts are all 51 mm (2 inches) in diameter. The test core assembly is fabricated by core drilling a standard LAS core at a 178 mm (7 inch) radius to accept the matrix inserts. The various size inserts (of various compositions) are secured into the host core with a hydraulic (calcium aluminate) cement. The faces of each insert are slightly recessed to protect against excessive crossarm and seal interference. Test cores such as this are run on either or both sides of a Ford 707 gas turbine engine which is ingesting road salt while operating at a regenerator inlet temperature of approximately 800°C (1472°F).

Figures III.B.2.2 and III.B.2.3 are photographs of the apparatus and plumbing used to insure the reproducible ingestion of road salt into the gas turbine engine. Every effort has been made to keep the salt dry, as moisture pick-up has been identified as the principal factor leading to restricted salt flow. Figure III.B.2.2 is a picture of the salt handling equipment located inside a monitor room immediately adjacent to the engine test pad. Throttled air is warmed by a heat lamp (a) before flowing through a vibrating hopper salt feed unit, (b) the hopper is kept filled with road salt which has been ground and sized for free flow. The interior of the hopper is kept dry by suspended containers of desiccant. The timer (d) controls the feed time of the hopper, and reproducible quantities of salt are transported, via the dry air stream, through a pipe (c) to the inlet (Figure III.B.2.3) of the Ford 707 gas turbine engine.



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Figure III.B.2.1 Accelerated Corrosion Test Core with Matrix Inserts

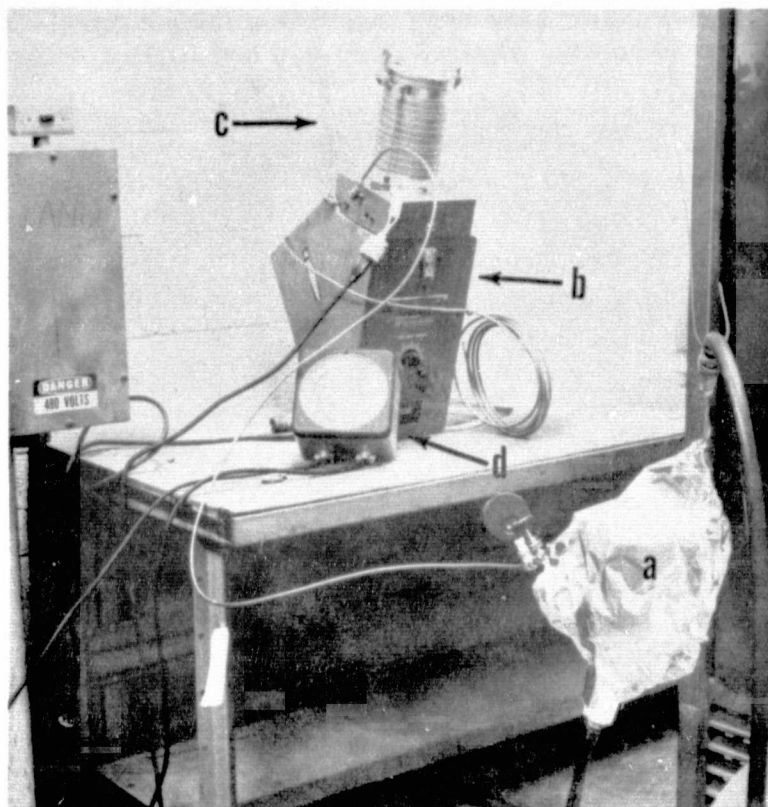


Figure III.B.2.2 Salt Ingestion System — Measurement and Transportation

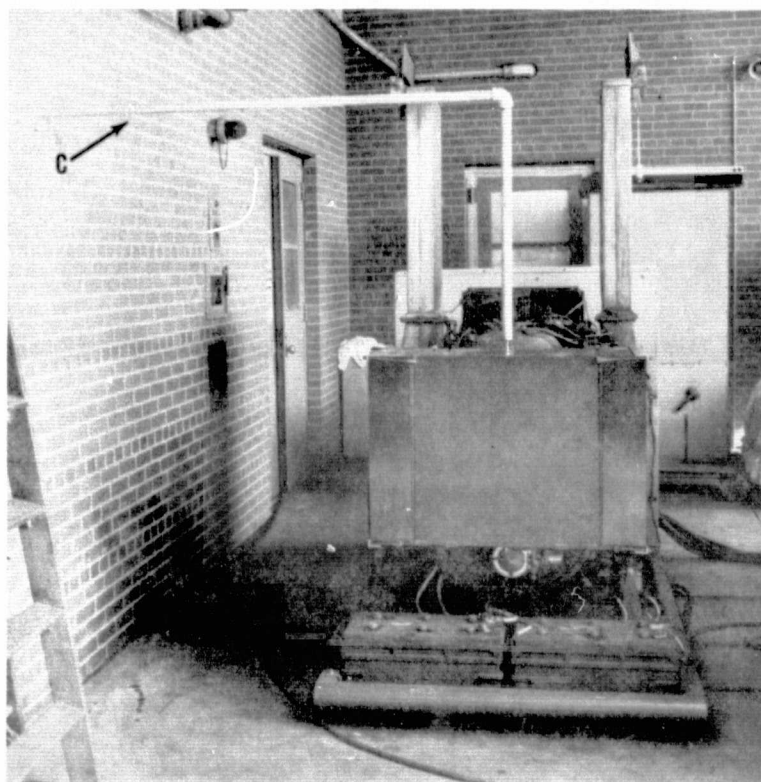


Figure III.B.2.3 Salt Ingestion System — Hook up to 707 Test Engine

A total of 59 test hours have been accumulated on these initial core inserts. Chemical samples of each insert have been taken from the hot and the cold faces at test intervals of 25 and 39 hours. Figures III.B.2.4 and III.B.2.5 illustrate the

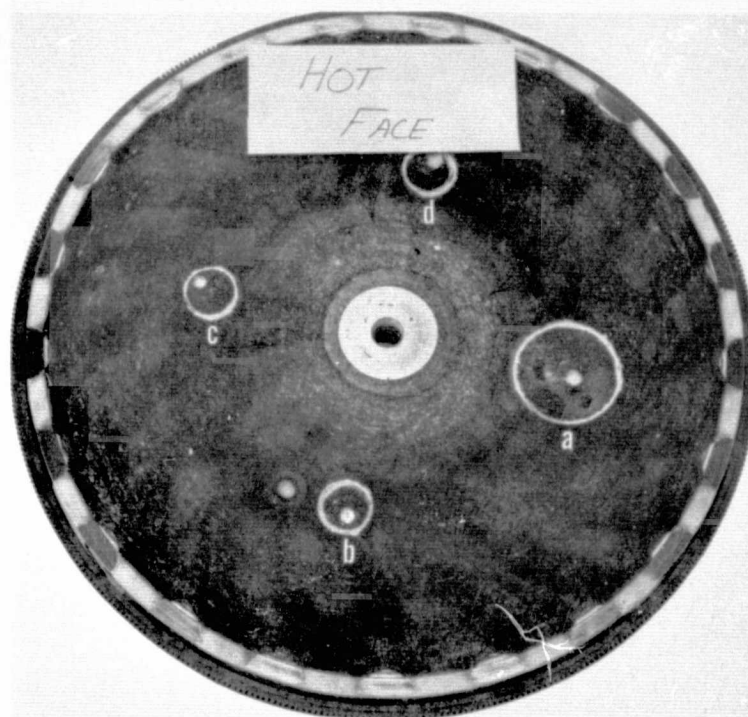
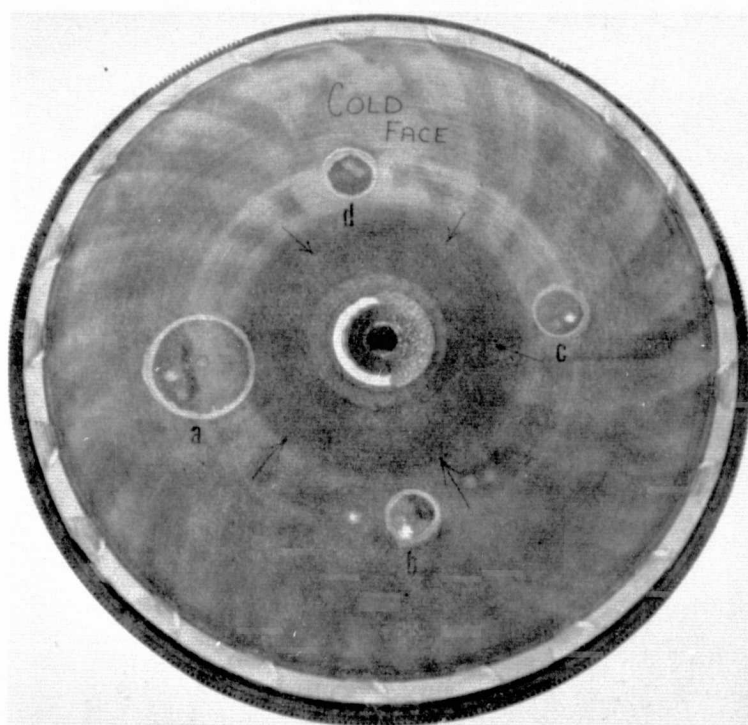


Figure III.B.2.4 Accelerated Corrosion Test Core Hot Face After 39 Hours of Salt Ingestion



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Figure III.B.2.5 Accelerated Corrosion Test Core Cold Face After 39 Hours of Salt Ingestion

condition of the hot face and the cold face, respectively, after 39 test hours. Sampling indentations are visible in each matrix, and one may see (especially evident on the cold face—Figure III. B. 2.5) the beginning of a typical sodium-induced failure in an LAS core. This serves to point out the severity of this test. The test samples taken to date are presently being analyzed.

III. B. 3 Accelerated Corrosion Testing — Full Size Core

No candidate material for this portion of the materials screening tests has been chosen.

III. C. PROBLEM AREAS

The accelerated corrosion testing of matrix inserts was begun early, as it seemed advantageous to gain some experience with the salt metering and transportation system. The advisability of this action was reinforced by the difficulties experienced to date. Some problems have been encountered with the salt metering system. Total gas system dew point has been sufficiently lowered so salt delivery and flow is very rapid. Initial system calibration, accomplished using nitrogen as the transport gas, had to be redone when air was substituted as the carrier fluid. Air pressure variations have resulted in a variable salt delivery rate, although only one excursion beyond the acceptable feed rate range (2.6–6.1 grams per hour) could be verified. Abiding by the test specification, those hours of excessive salt feed were doubly weighted; however, as Figure III. C. 2.1 illustrated, excessive salt feed rates lead to premature, catastrophic failure of the LAS host core. The core pictured was running (without test inserts) on the other side of the salt ingestion engine; however, two host core failures have been experienced in the 59 hours of accelerated corrosion test time. A core failure results in engine shut-down; and if the host core fails, the test inserts must be removed from the broken remains, cleaned, and re-mounted in a fresh host core. This operation is obviously time intensive.

The more precisely controllable salt feed should serve to mitigate these problems in the future.

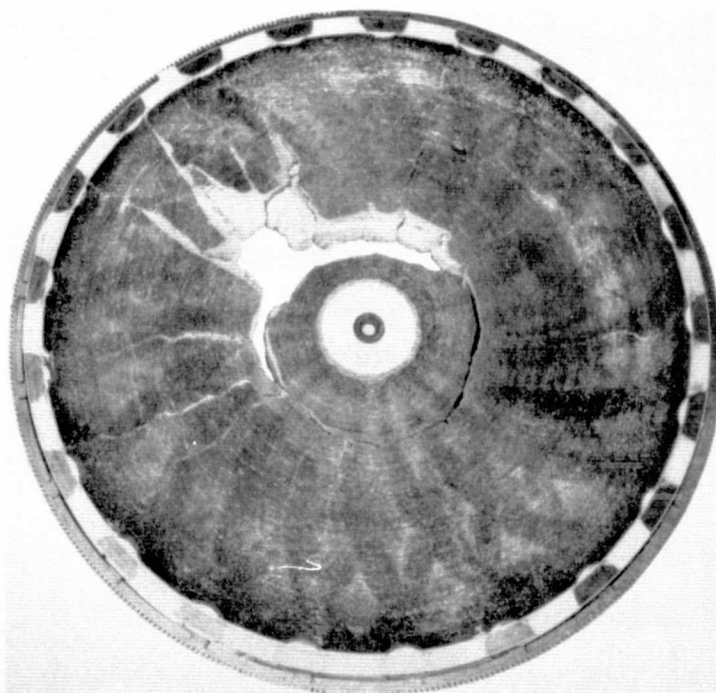


Figure III. C. 2.1 LAS Core After 59 Hours of Salt Ingestion

III.D. FUTURE PLANS

It is anticipated that the materials currently on laboratory test will be taken through this testing program during the next reporting period. Materials received during the next reporting period will be machined into test specimens, introduced into the laboratory test program, and reported as partially completed results. Efforts to secure the remaining materials candidates for this testing program will continue.

The matrix inserts currently on test should reach the test limit of 120 hours by the end of the next reporting period. A second group of matrix inserts are presently timed for engine testing starting during the next quarter.

No accelerated corrosion testing of full size cores is planned for the next reporting period.

III.E. TASK SUMMARY

The materials screening test portion of the ceramic regenerator development program is well under way. Preliminary data from the laboratory testing program (hot and cold face simulation) suggest that many of the new MAS and AS materials may be better than current material candidates. Accelerated corrosion testing of matrix inserts has progressed to the half-way point, and a subsequent chemical analysis of 25 and 39 engine hour specimens will indicate the degree of sodium attack experienced by 4 candidate materials. No significant problems have been encountered or are anticipated.

TASK IV. AEROTHERMODYNAMIC PERFORMANCE

IV.A. INTRODUCTION

The matrix fin configuration selected for a given heat exchanger, under specific engine conditions, has a significant influence on the level of thermal stress and thermodynamic performance. In order to evaluate thermodynamic performance of prospective fin configurations, a shuttle rig was designed and fabricated (Section Q in Reference 1).

The essential parameters required for accurate heat exchanger performance prediction are the basic heat transfer ($J = \text{Stanton-Prandtl No.} = \text{Colburn No.} = C_2 \text{ RE}^{X_2}$) and pressure drop ($F = \text{Fanning Friction Factor} = C_1 \cdot \text{RE}$) characteristics of the matrix fin geometry being evaluated as a function of a nondimensional flow parameter ($\text{RE} = \text{Reynold's No.}$). In order to obtain the basic heat transfer and pressure drop data, a transient technique similar to the "sliding drawer" technique described in Reference 3 was used.

The shuttle rig, shown in Figure IV.A.1 consists of two concentric cylinders fabricated from a material with extremely low thermal conductivity (acrylic plastic). The outer cylinder (Figure IV.A.2) consists of four square ports 90° apart, which contain pressure taps and thermocouples. The inner cylinder (Figure IV.A.3), which contains the matrix sample, is mechanically rotated 90° between the hot and cold air streams. The tight clearance between the concentric cylinders prevents the air from leaking from one stream to the other.

With the inner cylinder exposed to the hot air stream, the test matrix is heated to a uniform temperature of approximately 12°C (22°F) above ambient. A step change in fluid temperature is imposed on the test matrix by rotating the inner cylinder 90° . The downstream fluid temperature, which is referenced to the upstream fluid temperature, is monitored and recorded versus time. By determining the maximum slope of the fluid temperature difference curve during the cooling transient, the Colburn No. of the test matrix can be determined for each flow condition (Reynold's No.). The theoretical basis for this measurement technique is described in Reference 3.

In addition to the dependence on the maximum slope of the fluid temperature difference curve during the cooling transient, the level of the heat transfer characteristics (Colburn No.) is dependent on the fin parameter values utilized for data reduction. This was previously discussed in Section Q in Reference 1. In order to determine the pertinent fin configuration parameters (Figure IV.A.4) required for data reduction, an enlarged photo (Figure IV.A.5) of a 25.4 mm (1 inch) square section of the test matrix is taken. From this photo the number of openings per row per inch (X) and the number of rows per inch (Y) is determined. A computer program determines the open area (σ), hydraulic diameter (DH), and heat transfer surface area per unit volume (β) as a function of X , Y , fillet radius, and fin material thickness (S). After measuring the weight and volume of the test matrix, the open area (σ) is determined based on the wall density (ρ_w) of the matrix material. Once the open area is established, the remaining parameters (DH , β , S) are determined from the computer read-out for the X , Y , and S combination. Consequently, the pertinent fin parameters required (σ and DH) for data reduction are highly dependent on the accuracy of the wall density (ρ_w) value of the matrix material.

Erroneous estimates of the fin parameters (σ and DH) can introduce significant discrepancies in the F and J curves. Consequently, an alternate method of determining the heat transfer and pressure drop characteristics that eliminates the ne-

cessity of estimating fin parameters is desirable. In addition, the alternate characteristics will allow a direct comparison of test data from different sources, since a universal method of determining pertinent fin parameters is non-existent at this time. As previously presented in Section Q.6 of Reference 1, alternate pressure drop and heat transfer characteristics can be presented in the following forms:

$$\frac{\Delta P \cdot P}{L} = C \frac{WT}{A_F}^{1.673}$$

$$\frac{NTU}{L} = A \left[\frac{A_F T^{.673}}{W} \right]^{-X_2}$$

Where:

ΔP = Matrix pressure drop — KPa (PSI)

P = Fluid pressure — KPa (PSIA)

L = Flow length — CM. (In.)

W = Air Mass flow rate — Kg/Sec. (Lb./Sec.)

T = Fluid temperature — $^{\circ}K$. ($^{\circ}R$.)

A_F = Matrix frontal area — $M.^2$ (FT. 2)

σ = Open area ratio

C_1 = Fanning Friction Factor constant for laminar flow = $F \cdot RE$

DH = Hydraulic diameter — CM. (In.)

NTU = By definition the number of heat transfer units (determined from the maximum slope of the fluid temperature difference curve during the cooling transient as described in Reference 3)

C_2 = Colburn No. constant for laminar flow = J/RE^{X_2}

X_2 = Reynold's No. (RE) exponent from the Colburn No. ($J=C_2 RE^{X_2}$)

$$C = 3.56 (10^{-9}) \frac{C_1}{\sigma DH^2} \left[3.506 (10^{-10}) \frac{C_1}{\sigma DH^2} \right]$$

$$A = 4.98 \left[62.6 (10^{-7}) \right]^{-X_2} \frac{C_2 \sigma^{-X_2}}{DH^{(1-X_2)}} \left[4.98 \left[21.9 (10^{-7}) \right]^{-X_2} \frac{C_2 \sigma^{-X_2}}{DH^{(1-X_2)}} \right]$$

If $X_2 = -1$; then:

$$A = 3.11 (10^{-5}) \frac{C_2 \sigma}{DH^2} \quad \left[1.09 (10^{-5}) \frac{C_2 \sigma}{DH^2} \right]$$

Once the constants (C and A) have been determined from the equation of the line for the alternate performance characteristics, the pertinent constants (C_1 and C_2) for the basic performance characteristics can be determined from estimated values of σ and DH.

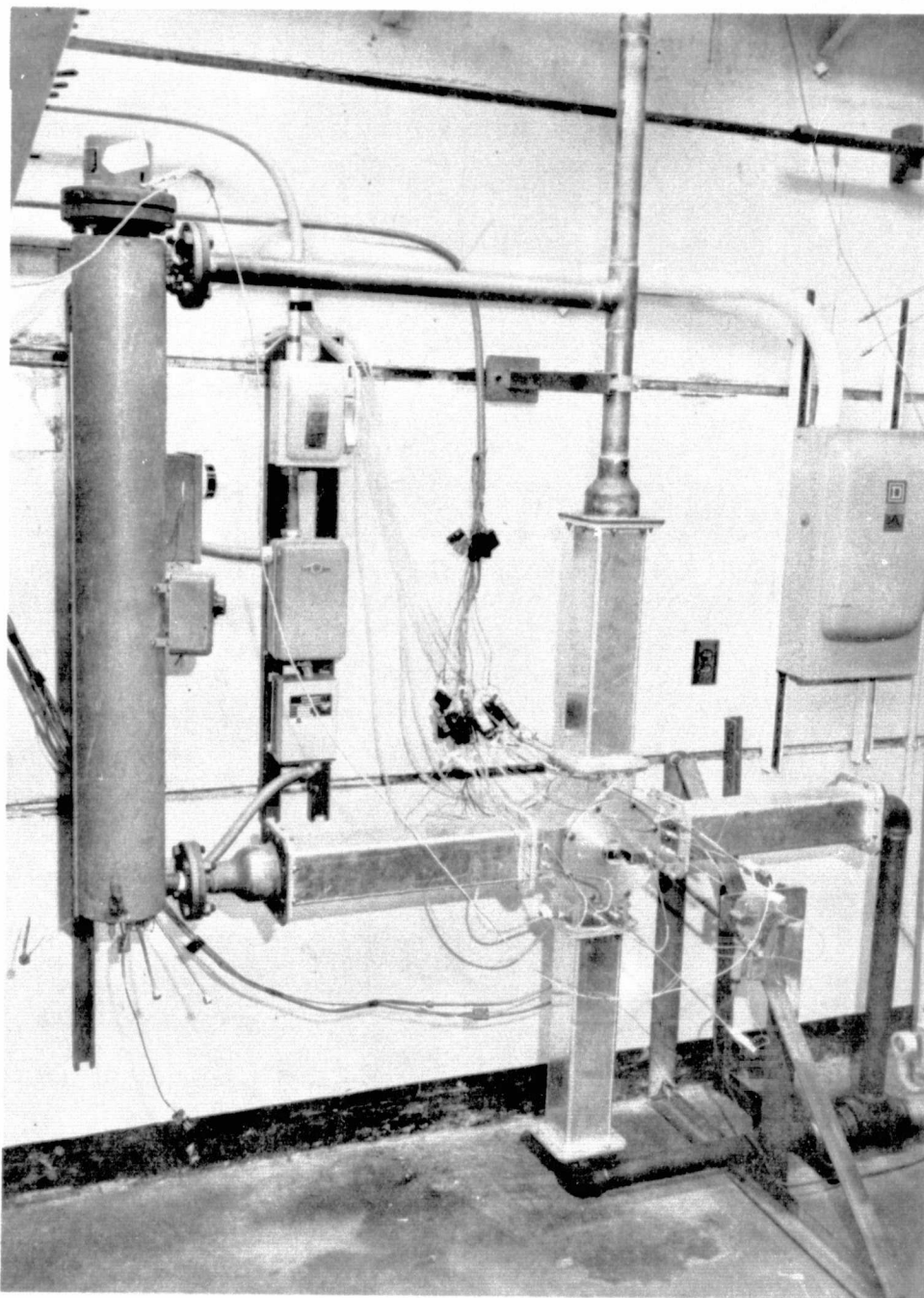


Figure IV. A.1 Photograph of Shuttle Rig

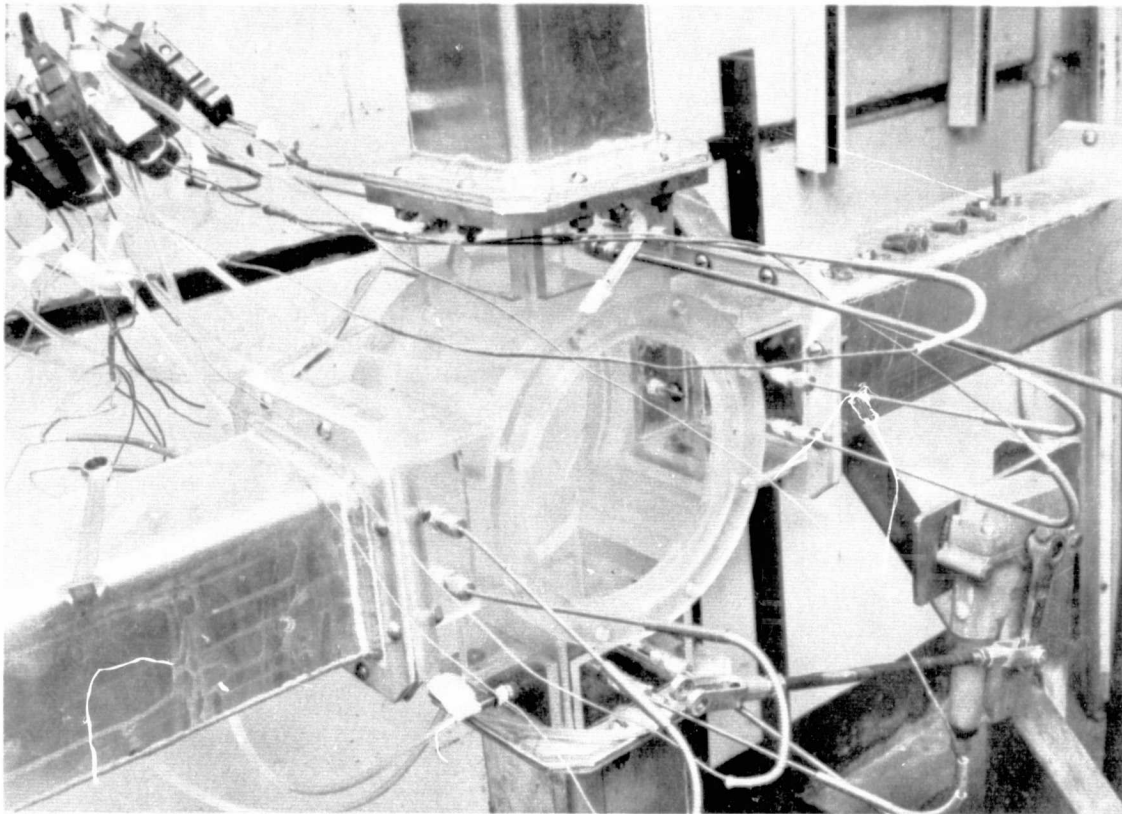


Figure IV. A. 2 Photograph of Outer Cylinder Used in the Shuttle Rig

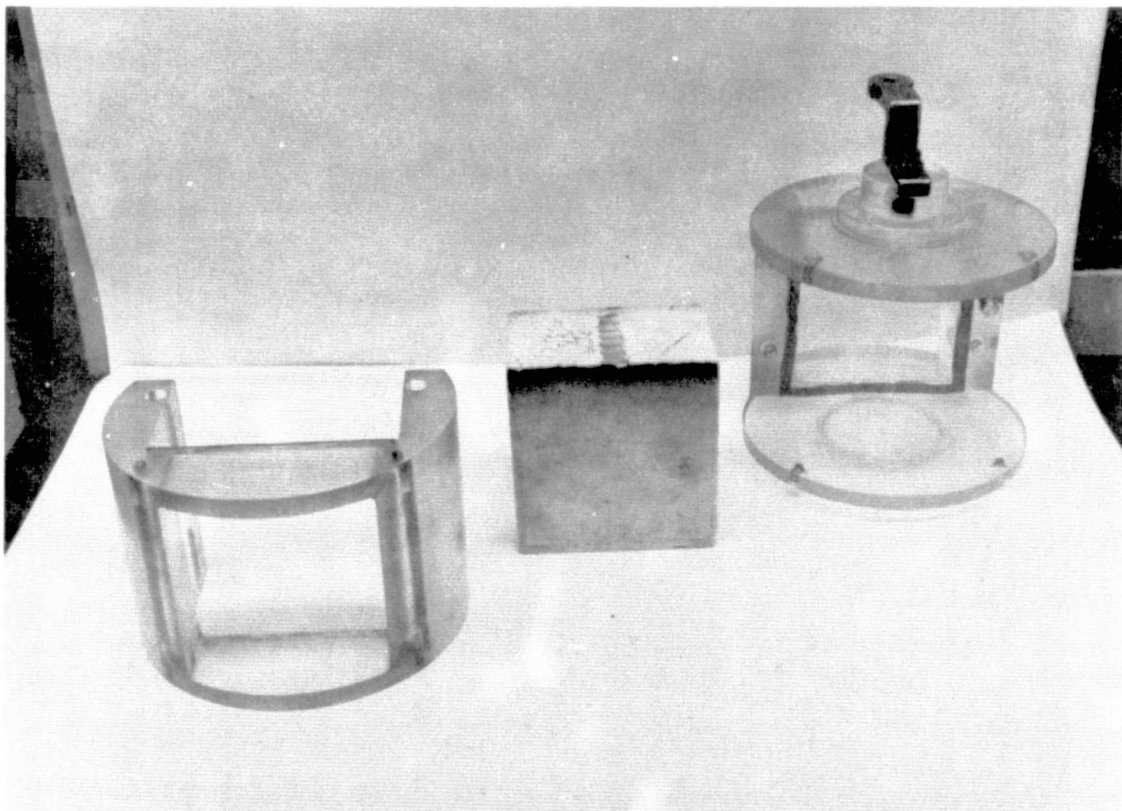
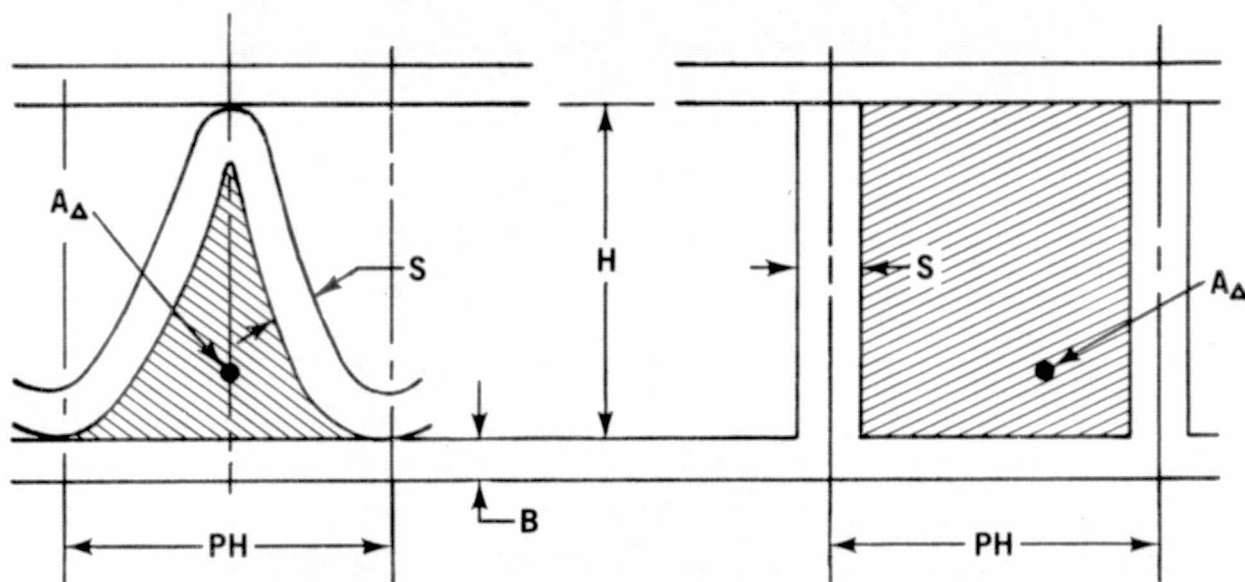


Figure IV. A. 3 Photograph of Inner Cylinder Used in the Shuttle Rig



X = NO. OF FINS PER ROW PER UNIT OF MEASURE

Y = NO. OF ROWS PER UNIT OF MEASURE

H = FIN HEIGHT = $\frac{1}{Y} - B$

PH = FIN PITCH = $\left(\frac{1}{X}\right)$ RECTANGULAR = $\left(\frac{2}{X}\right)$ TRIANGULAR

A.R. = ASPECT RATIO = PH/H

B = SHEET THICKNESS

S = FIN THICKNESS

A_{Δ} = CROSS-SECTIONAL AREA OF ONE OPENING

W.P. = WETTED PERIMETER OF ONE OPENING

DH = HYDRAULIC DIAMETER = $4 \cdot A_{\Delta} / W.P.$

σ = RATIO OF OPEN AREA TO FRONTAL AREA

$$\sigma_{\text{TRIANGULAR}} = \frac{2 \cdot A_{\Delta}}{PH (H+B)} \quad \sigma_{\text{RECTANGULAR}} = \frac{H (PH-S)}{(H+B) (PH)}$$

β = HEAT TRANSFER SURFACE AREA PER UNIT VOLUME = $\frac{4 \cdot \sigma}{DH}$

Figure IV. A. 4 Definition of Fin Parameters for Sinusoidal and Rectangular Flow Passages

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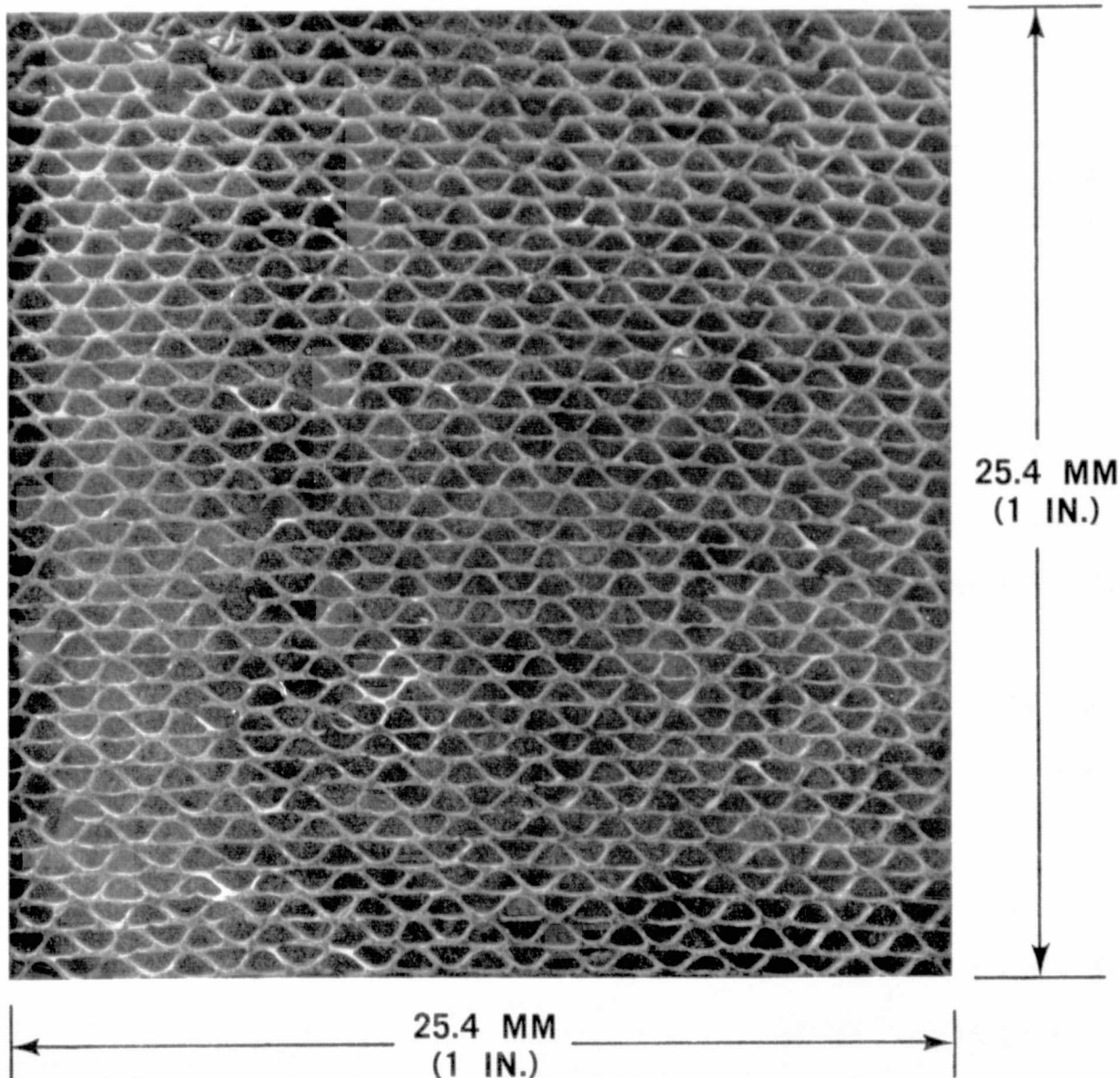


Figure IV.A.5 Photograph Showing One Inch Square Section from Test Matrix

IV.B. STATUS

IV.B.1 Matrix Test Samples

The heat transfer and pressure drop characteristics for the first twenty matrix fin configurations evaluated in the shuttle rig were presented in Reference 2 (Section D, Table D.2.1). An additional six matrix fin configurations were evaluated during this report period.

Prior to the evaluation of the new matrix surfaces, refinements for the previous data were incorporated. The fin parameters required for data reduction of the previous test samples were based on a first approximation of equivalent rib and web

thickness for the embossed structures. Closer examination of the photo enlargements of these samples revealed the rib to be .60 to 1.70 the thickness of the web on four of the embossed structures (core No. 8, 9, 14 and 15). Consequently, the fin parameters for these configurations were re-evaluated with the heat transfer and pressure drop characteristics adjusted accordingly.

In addition, the cold side thermocouple was moved closer to the exit plane as shown on figure IV.B.1.1. In effect, the thermal response will be improved slightly for the samples evaluated with a smaller cross section than the standard 10.2 cm x 10.2 cm (4 inch x 4 inch) opening. Therefore the samples (core No. 6, 8, and 12 thru 17) with the smaller cross-sectional area were re-evaluated and the heat transfer characteristics adjusted accordingly.

The complete tabulation for the first twenty matrices evaluated incorporating the above refinements is listed on Table IV.B.1.1. Subsequent matrices have been evaluated in the same manner.

The present matrix sample size (including the previous twenty matrices) listed in Tables IV.B.1.1 and IV.B.1.2 contains fifteen rectangular (core no. 2, 3, 6, 8, 9, 12 thru 17 and 21 thru 24), one hexagonal (core no. 26), eight sinusoidal (core no. 1, 5, 7, 10, 11, 19, 20 and 25) and two isosceles triangular (core no. 4 and 18) fin configurations. In addition, the present sample size represents a good cross-section of the different manufacturing processes, which are currently being evaluated as follows:

1. Supplier A — Corrugating from a wet paper carrier or extrusion.
2. Supplier C — Corrugating from a plastic binder carrier.
3. Supplier D and E — Calendering
4. Supplier I — Extrusion

The first four matrices (Figures IV.B.1.2 thru IV.B.1.5) evaluated during this report period were fabricated by Supplier E utilizing the calendering method. Figures IV.B.1.6 to IV.B.1.9 illustrates the standard heat transfer (J) and pressure drop (F) characteristics for these matrices, which depend on the values determined for open area ratio (σ) and hydraulic diameter (DH). The alternate heat transfer and pressure drop characteristics, which are based on measured test data, are shown on Figures IV.B.1.10 to IV.B.1.13 for these matrices.

Supplier C fabricated matrix no. 25 (Figure IV.B.1.14) using a new set of tooling to produce a sinusoidal triangular structure similar to their initial structure (core no.5). The standard and alternate aerothermodynamic performance characteristics are illustrated on Figures IV.B.1.15 and IV.B.1.16, respectively.

Supplier I extruded matrix no. 26 (Figure IV.B.1.17) for evaluation. The standard and alternate aerothermodynamic performance characteristics for this hexagonal configuration are illustrated on Figures IV.B.1.18 and IV.B.1.19, respectively.

In the last report period (Reference 2), the following topics were discussed in detail based on the matrix sample size (20) in existence at that time:

1. The effect of manufacturing process on fin efficiency.
2. The effect of matrix fin geometry on performance.
3. The influence of aspect ratio on rectangular and triangular structures.

In light of the refinements to the data of the original sample size, the previous observations and trends for these topics will be reviewed and supplemented with the additional matrix data accumulated during this report period.

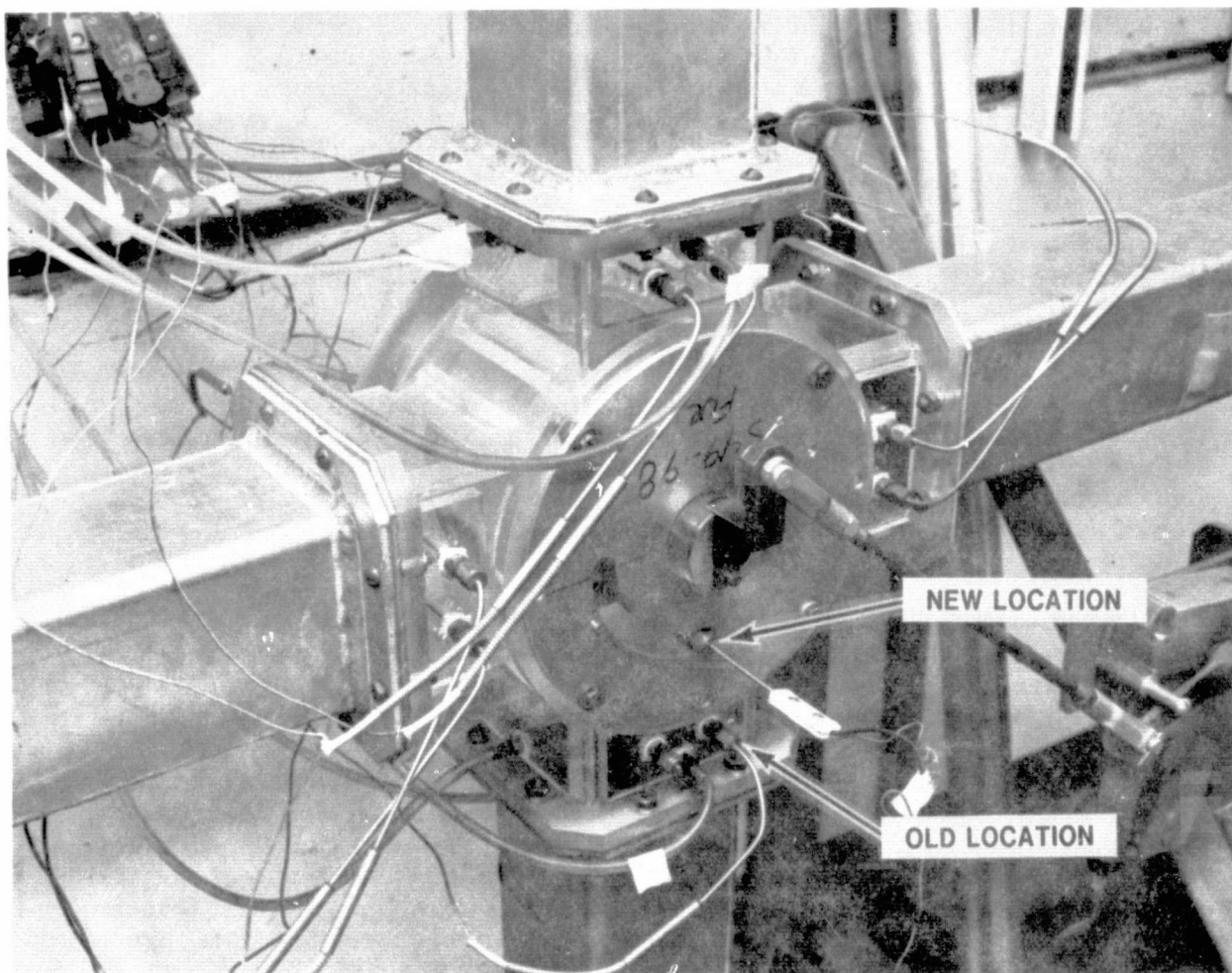


Figure IV.B.1.1 Photograph Showing Location of Shuttle Rig Instrumentation

R — RECTANGULAR
I.T. — ISOSCELES TRIANGULAR
S.T. — SINUSOIDAL TRIANGULAR
H — HEXAGONAL

$$F = C_1 REX^1$$

$$J = C_2 REX^2$$

$$\frac{\Delta P \cdot P}{L} = C \frac{WT^{1.673}}{AF}$$

$$\frac{NTU}{L} = A \left[\frac{AFT^{0.673}}{W} \right]^{-X_2}$$

CORE NO.	SUP-PLIER	TYPE OF FIN	X FINS CM.	Y ROWS CM.	N HOLES CM.2	S MM. (IN.)	B MM. (IN.)	H MM. (IN.)	PH MM. (IN.)	A.R. PH H	P W CC.	L DH	σ	DH MM. (IN.)	β M.2 M.3	C X 10 ⁵	A X 10 ²	C ₁	X ₁	C ₂	X ₂	J/F RE = 100	A _F CM. ² (IN. ²)
			(FINS IN.)	(ROWS IN.)	(HOLES IN.2)						(LB FT.3)				(FT.2 FT.3)								
1	A	S.T.	11.0 (28)	13.0 (33)	143 (924)	.109 (.0043)	.109 (.0043)	.660 (.0260)	1.813 (.0714)	2.75	2.20 (137.0)	121	.673	.589 (.0232)	4566 (1392)	1.30	4.55	13.4	-1	3.34	-1	.249	103 (16)
2	D	R	11.4 (29)	12.2 (31)	140 (900)	.193 (.0078)	.193 (.0078)	.627 (.0247)	.879 (.0348)	1.40	1.55 (97.0)	107	.599	.655 (.0258)	3654 (1114)	1.23	4.11	14.0	-1	4.19	-1	.299	103 (16)
3	D	R	8.3 (21)	8.7 (22)	71.6 (462)	.216 (.0085)	.216 (.0085)	.940 (.0370)	1.211 (.0477)	1.29	1.49 (92.8)	72	.669	.968 (.0381)	2765 (843)	.54	2.10	14.9	-1	4.18	-1	.281	96 (15.2)
4	I	I.T.	15.8 (40)	9.1 (23)	142.6 (920)	.135 (.0053)	.135 (.0053)	.970 (.0382)	1.270 (.0500)	1.31	1.56 (97.1)	121	.642	.589 (.0232)	4356 (1328)	1.11	5.11	10.9	-1	3.93	-1	.361	72 (11.2)
5	C	S.T.	9.9 (25)	15.4 (39)	151 (975)	.135 (.0053)	.135 (.0053)	.516 (.0203)	2.032 (.0800)	3.95	1.43 (89.0)	179	.569	.401 (.0158)	5688 (1734)	2.25	10.2	9.1	-1	.62	-.8	.171	103 (16)
6*	D	R	6.9 (17.5)	7.5 (19)	51.5 (332)	.300 (.0118)	.300 (.0118)	1.036 (.0408)	1.450 (.0571)	1.40	1.47 (91.4)	67	.615	1.092 (.0430)	2253 (687)	.515	1.55	16.7	-1	4.28	-1	.256	46 (7.1)
7	C	S.T.	5.1 (13)	6.3 (16)	32.2 (208)	.178 (.0070)	.178 (.0070)	1.410 (.0555)	3.908 (.1538)	2.77	1.51 (94.3)	53	.737	1.316 (.0518)	2240 (683)	.33	17.4	18.6	-1	.324	-.5	.174	103 (16)
8*	E	R	4.1 (10.5)	14.4 (36.5)	59.4 (383)	.284 (.0112)	.213 (.0084)	.482 (.019)	2.418 (.0952)	5.0	2.40 (149.5)	89	.611	.787 (.0310)	3103 (946)	1.10	3.85	18.4	-1	5.55	-1	.302	60 (9.3)
9*	E	R	5.3 (13.5)	15.8 (39.5)	82.6 (533)	.386 (.015)	.228 (.009)	.411 (.0162)	1.88 (.074)	4.57	2.34 (146.3)	109	.505	.645 (.0254)	3129 (954)	1.89	4.29	17.5	-1	5.0	-1	.287	101 (15.6)
10	A	S.T.	14.6 (37)	14.4 (36.5)	209.5 (1351)	.066 (.0026)	.066 (.0026)	.630 (.0248)	1.374 (.0541)	2.18	2.09 (130.4)	128	.767	.549 (.0216)	5599 (1707)	1.60	5.9	16.3	-1	3.29	-1	.202	103 (16)
11	A	S.T.	8.3 (18)	10.2 (26)	64.5 (416)	.127 (.0050)	.127 (.0050)	.851 (.0335)	3.175 (.1250)	3.73	2.20 (137.3)	91	.720	.775 (.0305)	3716 (1133)	.76	2.35	14.5	-1	2.79	-1	.192	103 (16)
12*	A	R	7.0 (17.5)	7.1 (18)	49.6 (320)	.290 (.0114)	.290 (.0114)	1.123 (.0442)	1.427 (.0562)	1.27	1.67 (104.2)	62	.634	1.130 (.0445)	2244 (684)	.46	1.75	16.5	-1	5.0	-1	.303	66 (10.2)
13*	A	R	5.6 (14.2)	5.6 (14.2)	31.3 (202)	.274 (.0108)	.274 (.0108)	1.514 (.0596)	1.788 (.0704)	1.18	1.63 (101.5)	47	.718	1.487 (.0586)	1896 (578)	.25	4.43	18.2	-1	1.44	-.75	.250	66 (10.2)
14*	E	R	6.1 (15.5)	8.3 (21)	50.5 (326)	.305 (.012)	.381 (.015)	.837 (.033)	1.64 (.0645)	1.98	2.39 (149.3)	70	.557	1.02 (.0402)	2181 (665)	.62	2.3	15.9	-1	6.12	-1	.385	62 (9.6)
15*	E	R	6.1 (15.5)	11.0 (28)	67.3 (434)	.190 (.0075)	.330 (.013)	.584 (.023)	1.638 (.0645)	2.85	2.34 (145.8)	85	.558	.822 (.0324)	2711 (827)	1.00	4.04	16.7	-1	1.86	-.87	.212	64 (9.9)
16*	D	R	6.6 (16.8)	6.7 (17.0)	44.2 (286)	.315 (.0124)	.315 (.0124)	1.179 (.0484)	1.511 (.0597)	1.28	1.51 (94.2)	60	.625	1.186 (.0467)	2106 (642)	.44	1.30	17.1	-1	4.16	-1	.243	46 (7.1)
17*	E	R	3.2 (8.0)	15.4 (39.0)	49.1 (312)	.360 (.0142)	.241 (.0095)	.409 (.0161)	3.175 (.125)	7.76	2.31 (144.4)	98	.560	.713 (.0281)	3139 (957)	1.45	8.0	18.3	-1	3.08	-.88	.293	64 (9.9)
18	A	I.T.	7.9 (20)	4.7 (12)	37.4 (240)	.257 (.0101)	.257 (.0101)	1.859 (.0732)	2.540 (.1000)	1.37	1.53 (95.5)	60	.653	1.183 (.0458)	2244 (684)	.40	6.20	15.6	-1	1.00	-.72	.233	101 (15.6)
19	A	S.T.	15.0 (38)	13.6 (34.5)	203.9 (1311)	.061 (.0024)	.061 (.0024)	.875 (.0266)	1.335 (.0526)	1.98	2.10 (131.0)	122	.787	.579 (.0228)	5422 (1653)	1.38	6.50	16.1	-1	3.94	-1	.245	103 (16)
20	A	S.T.	10.8 (27.5)	14.6 (37)	157.4 (1018)	.066 (.0026)	.066 (.0026)	.820 (.0244)	1.846 (.0727)	2.98	2.18 (136.3)	107	.780	.592 (.0233)	5264 (1605)	1.36	5.03	16.4	-1	3.19	-1	.195	66 (10.2)

* DENOTES DATA REVISED SINCE LAST REPORT PERIOD

Table IV. B.1.1 Shuttle Rig Matrices Reported in Reference 2

CODE:

R — RECTANGULAR
I.T. — ISOSCELES TRIANGULAR
S.T. — SINUSOIDAL TRIANGULAR
H — HEXAGONAL

$$F = C_1 R E X_1$$

$$J = C_2 R E X_2$$

$$\frac{\Delta P.P}{L} = C \frac{WT^{1.673}}{AF}$$

$$\frac{NTU}{L} = A \left[\frac{AFT^{0.673}}{W} \right]^{-X_2}$$

CORE NO.	SUP-PLIER	TYPE OF FIN	X FINS CM	Y ROWS CM	N HOLES CM ²	S MM (IN.)	B MM (IN.)	H MM (IN.)	PH MM (IN.)	AR PH H	PW gM CC	L DH	σ	DH MM (IN.)	β M ² M ³	C X10 ⁵	A X10 ³	C ₁	X ₁	C ₂	X ₂	J F @ RE-100	AF CM ² (IN. ²)
			(FINS IN.)	(ROWS IN.)	(HOLES IN. ²)						(LB. FT. ³)				(FT. ² FT. ³)								
21	E	R	5.6 (14.3)	10 (25.3)	56.1 (362)	.223 (.0088)	.279 (.011)	.724 (.0285)	1.78 (.070)	2.45	2.02 (126.2)	71	.637	.991 (.0390)	2572 (784)	.62	4.70	17.1	-1	1.80	-.83	.222	103 (16)
22	E	R	3.9 (10)	15.4 (39)	60.5 (390)	.277 (.0109)	.208 (.0082)	.442 (.0174)	2.54 (.100)	5.75	2.29 (143.2)	94	.608	.739 (.0291)	3290 (1003)	1.28	4.20	18.8	-1	5.37	-1	.286	103 (16)
23	E	R	2.4 (6.2)	16.7 (42.3)	40.6 (262)	.381 (.015)	.191 (.0075)	.406 (.016)	4.09 (.161)	10.0	2.39 (149.1)	95	.618	.737 (.0290)	3355 (1023)	1.43	8.64	21.2	-1	1.62	-.81	.184	103 (16)
24	E	R	4.7 (12)	12.6 (32)	59.5 (384)	.417 (.0164)	.279 (.0110)	.516 (.0203)	2.12 (.0833)	4.10	1.94 (121.0)	89	.521	.790 (.0311)	2637 (804)	1.18	2.45	16.9	-1	4.17	-1	.247	103 (16)
25	C	S.T.	9.8 (25)	12.8 (32.5)	126 (813)	.183 (.0072)	.183 (.0072)	.599 (.0236)	2.03 (.0800)	3.39	2.33 (145.7)	153	.512	.417 (.0164)	4907 (1496)	2.4	4.3	9.4	-1	2.07	-1	.220	103 (16)
26	I	H	6.8 (17.3)	5.9 (15)	40.3 (260)	.323 (.0127)	.323 (.0127)	1.37 (.054)	1.47 (.058)	1.07	1.62 (101.3)	51	.656	1.37 (.054)	1912 (583)	.32	1.1	17.4	-1	4.48	-1	.257	46 (7.1)

Table IV. B. 1. 2 Shuttle Rig Matrices Tested in Current Report Period

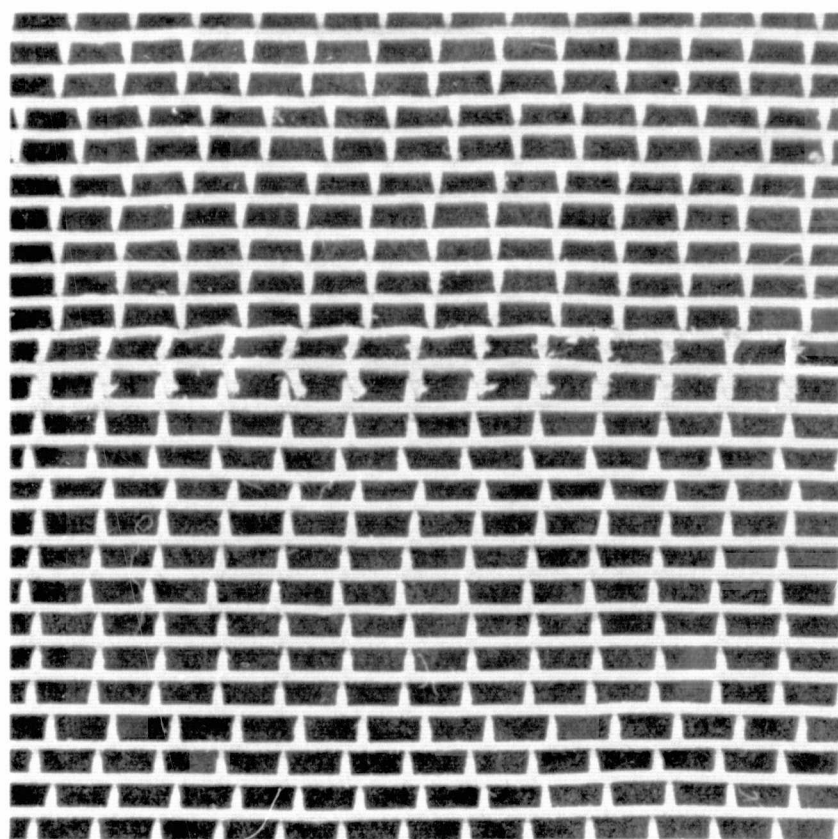


Figure IV.B.1.2 Photograph of Matrix No. 21

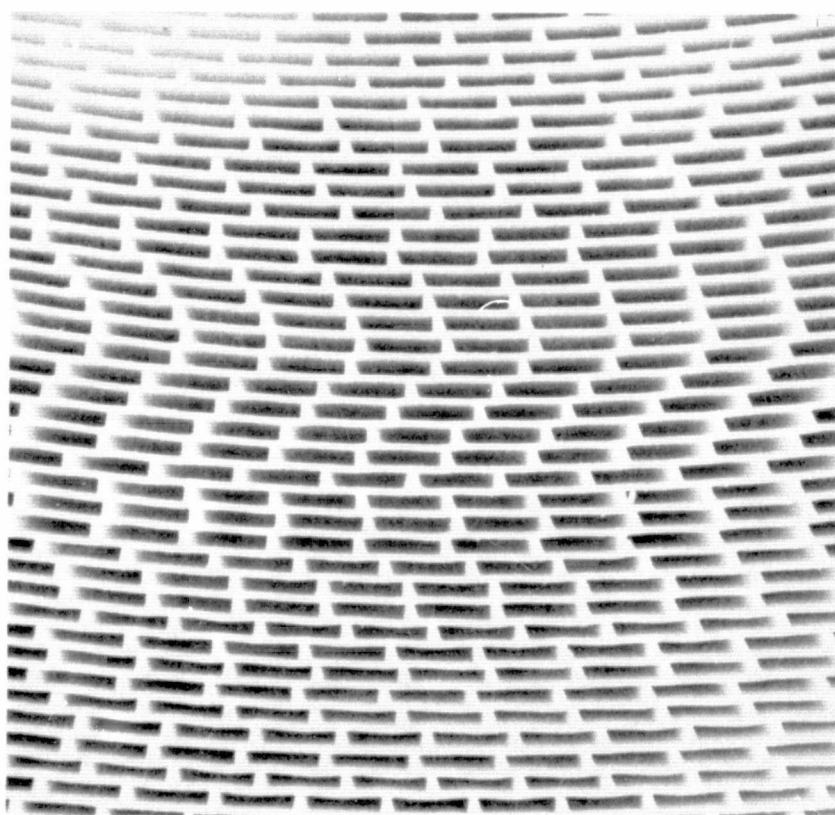


Figure IV.B.1.3 Photograph of Matrix No. 22

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Figure IV.B.1.4 Photograph of Matrix No. 23

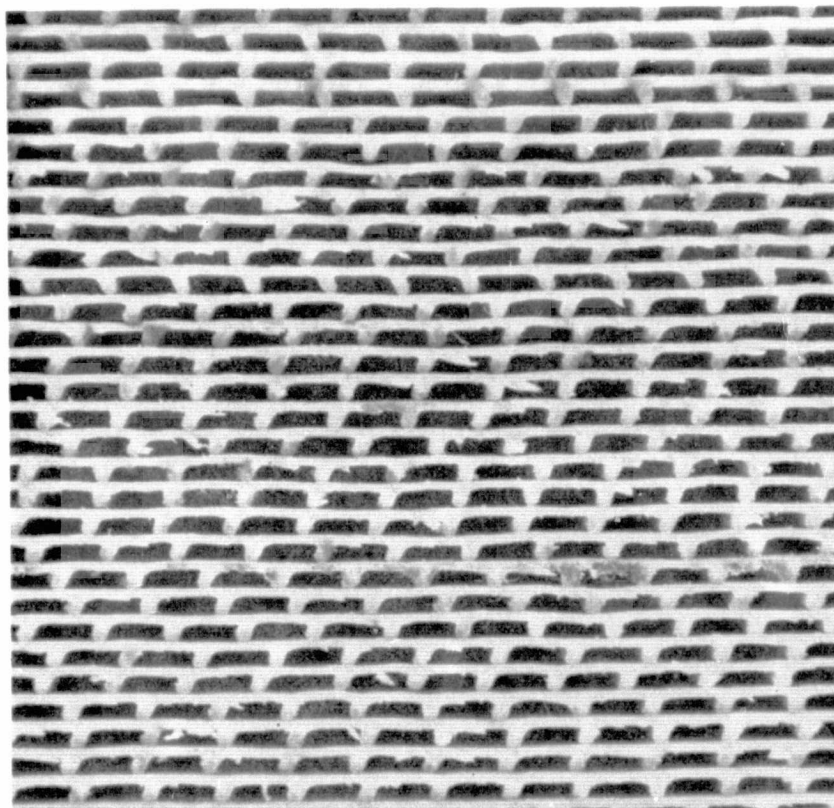


Figure IV.B.1.5 Photograph of Matrix No. 24

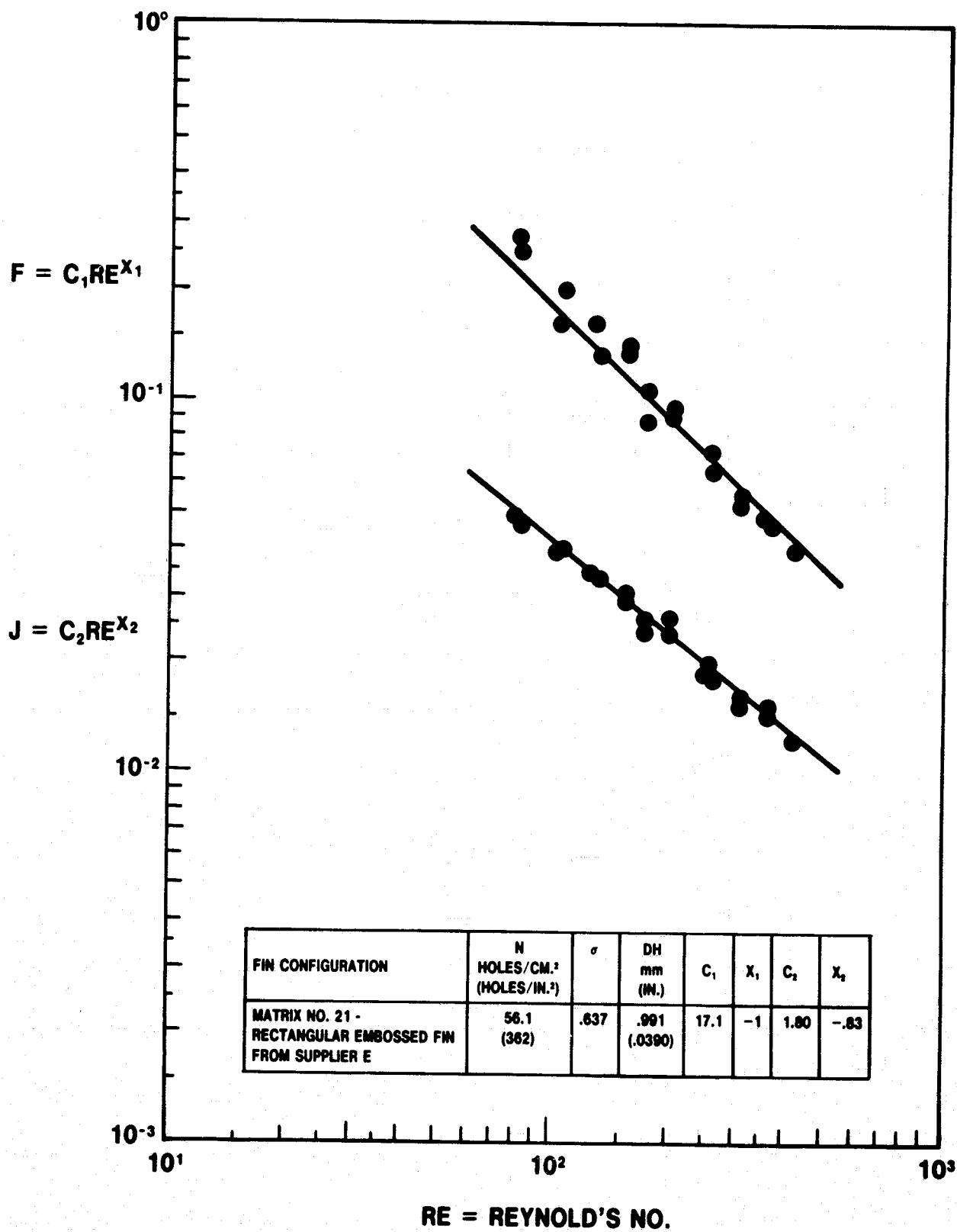


Figure IV.B.1.6 Standard Thermodynamic Performance Characteristics of Matrix No. 21

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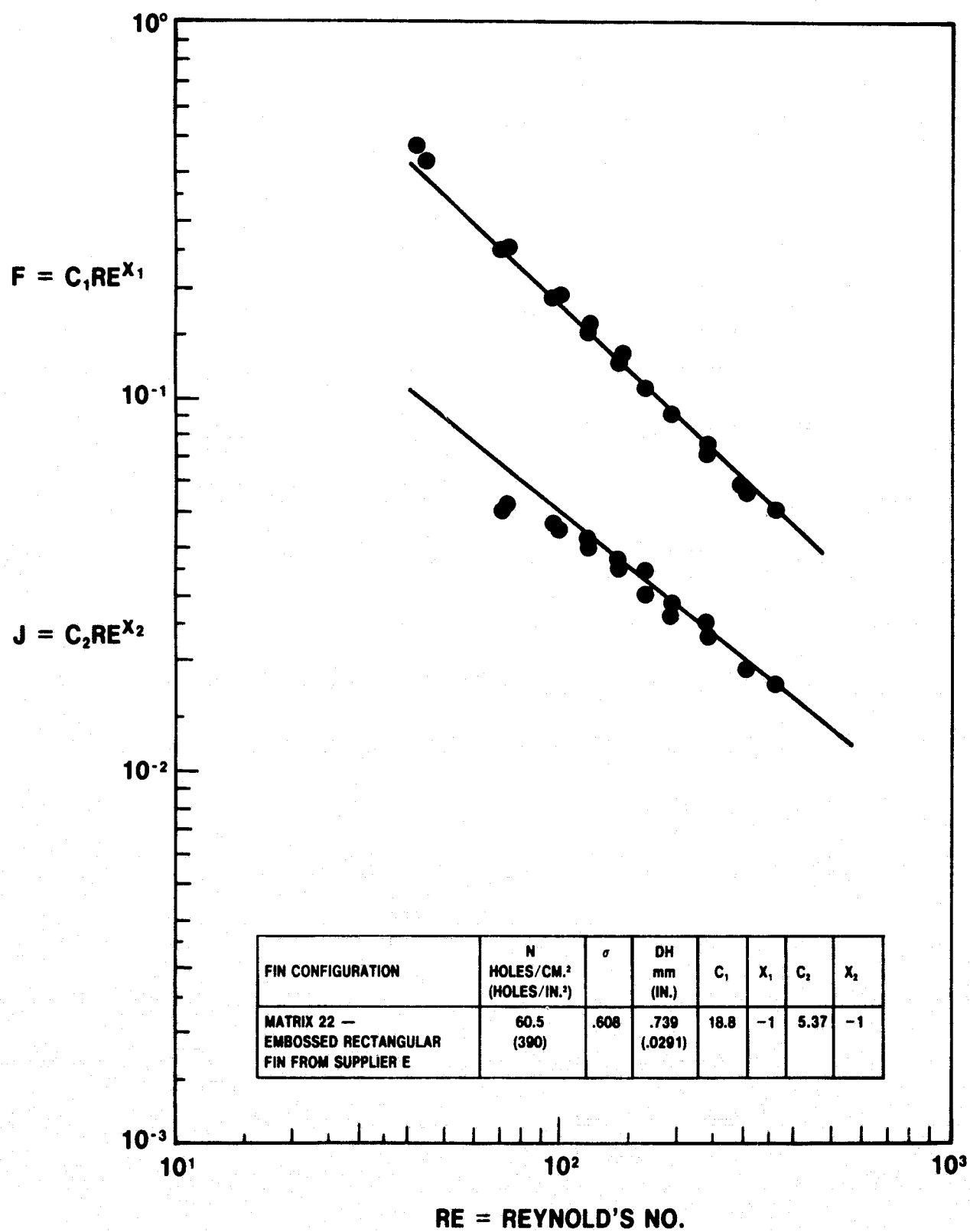


Figure IV.B.1.7 Standard Thermodynamic Performance Characteristics of Matrix No. 22

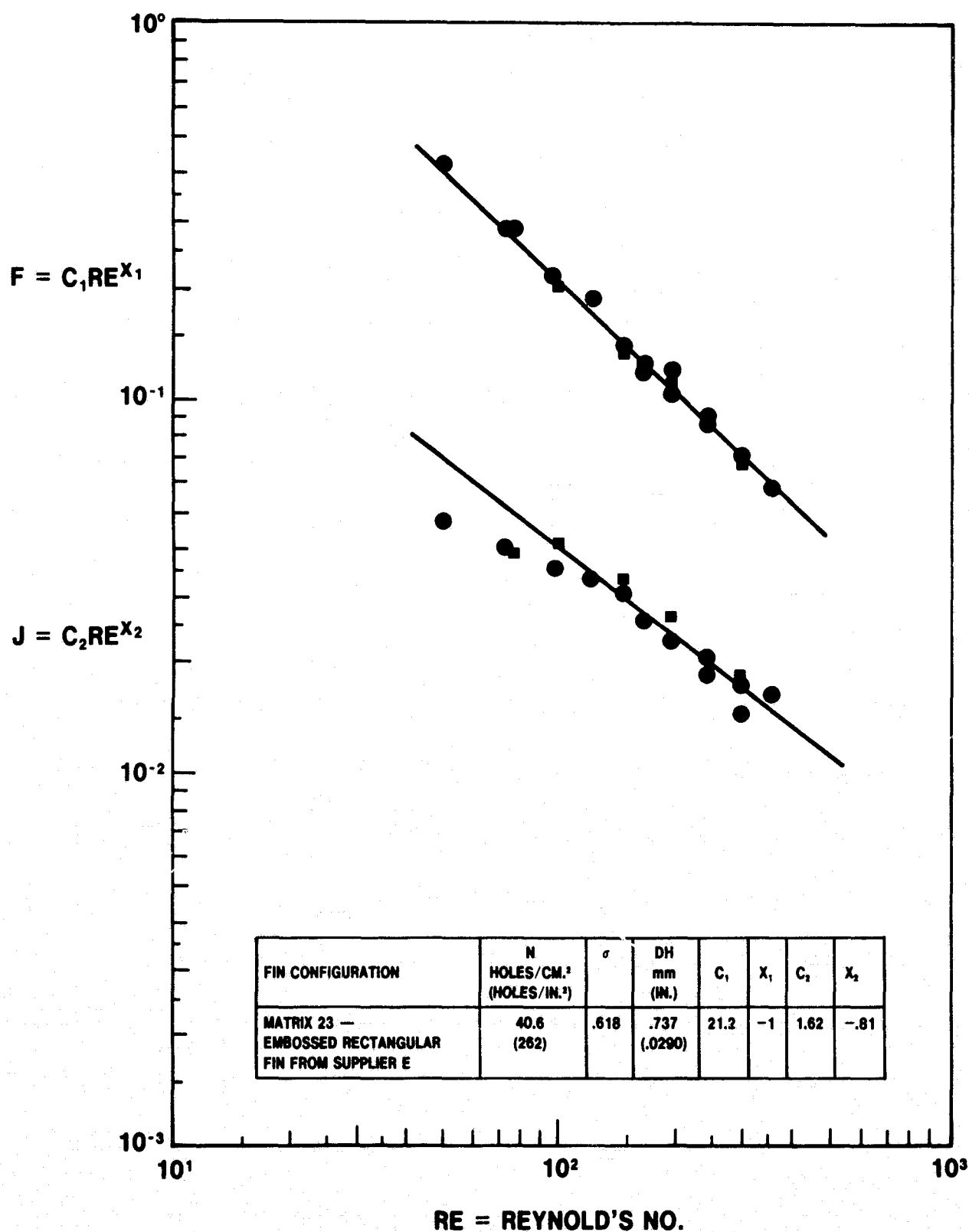


Figure IV.B.1.8 Standard Thermodynamic Performance Characteristics of Matrix No. 23

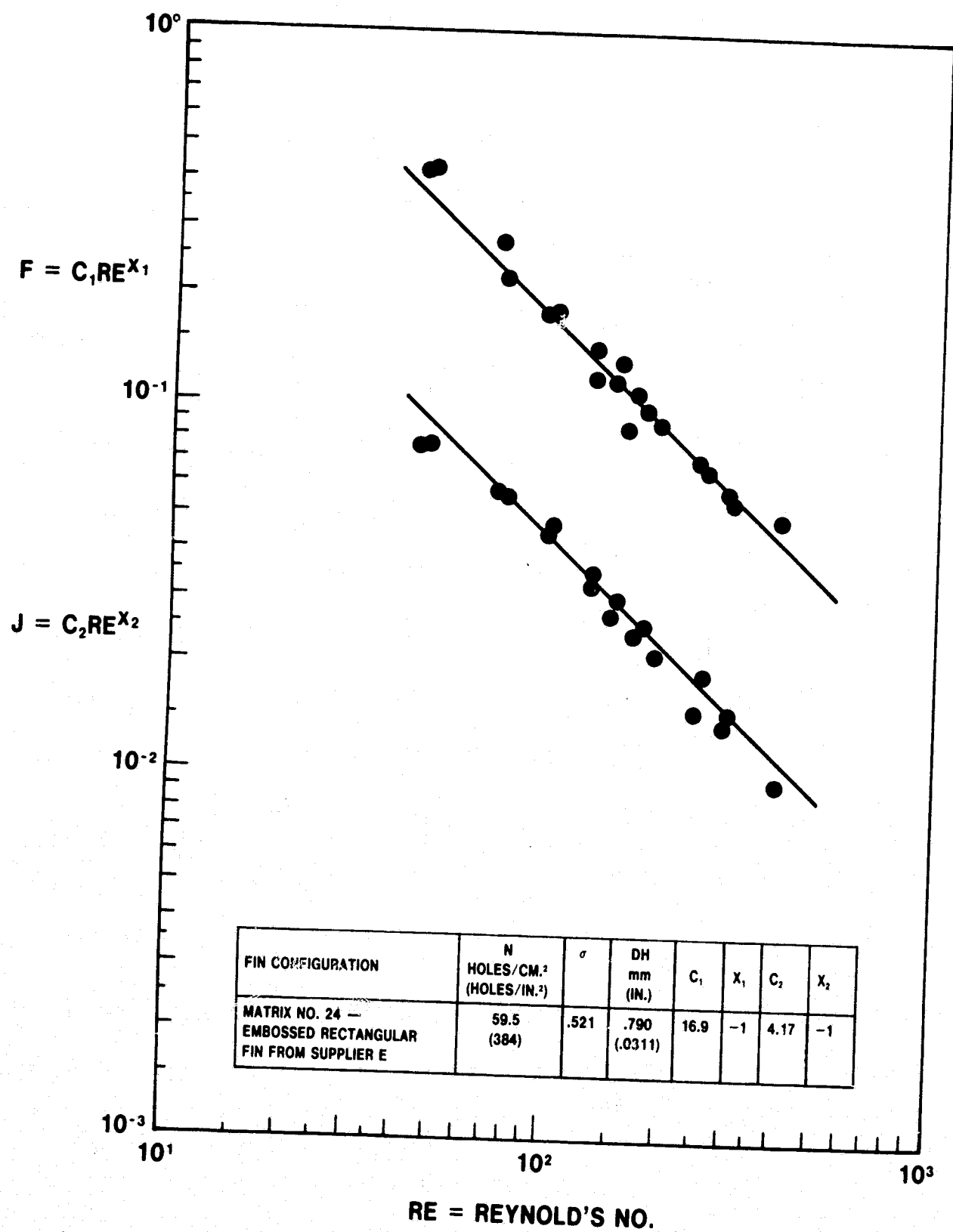


Figure IV.B.1.9 Standard Thermodynamic Performance Characteristics of Matrix No. 24

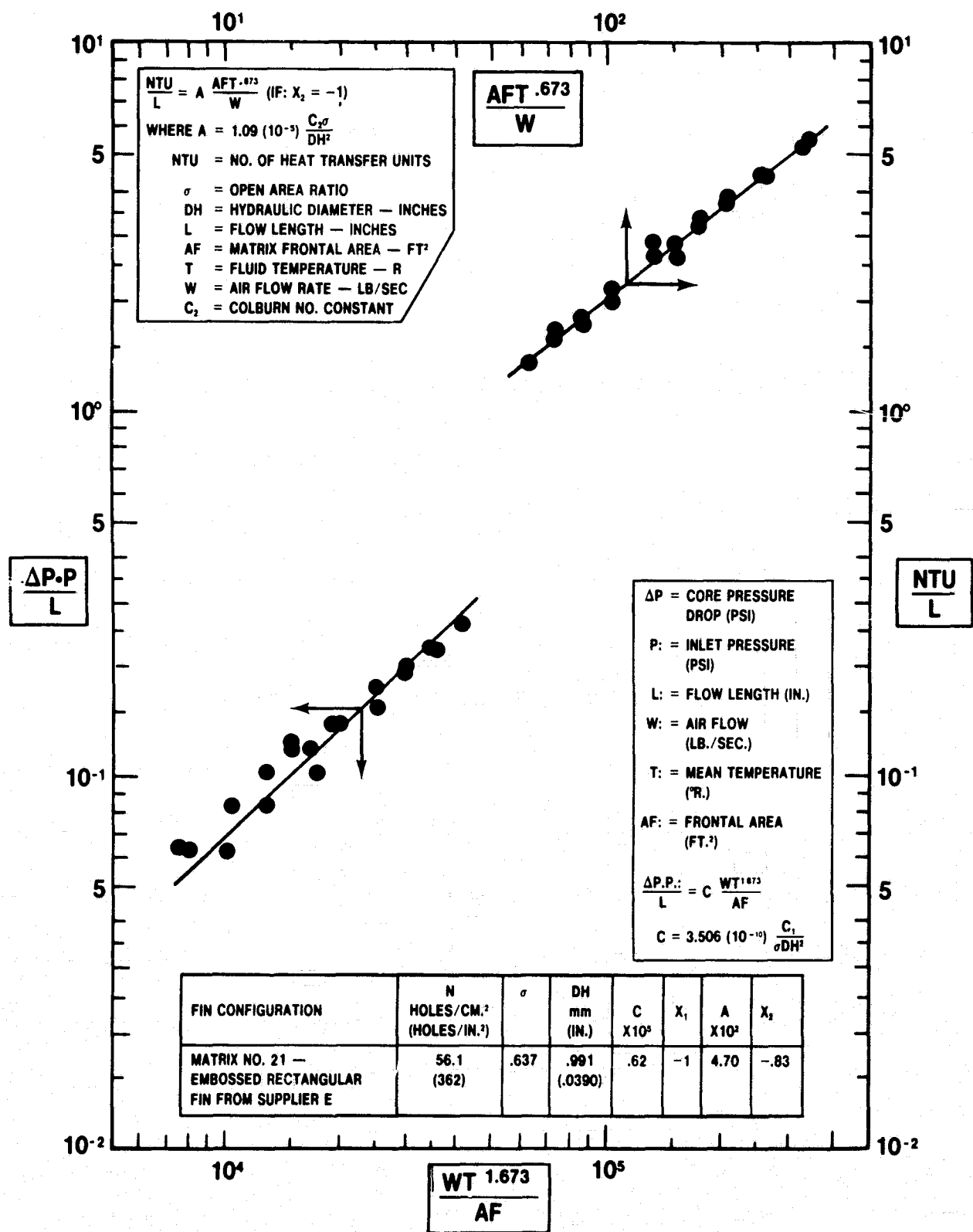


Figure IV.B.1.10 Alternate Thermodynamic Performance Characteristics of Matrix No. 21

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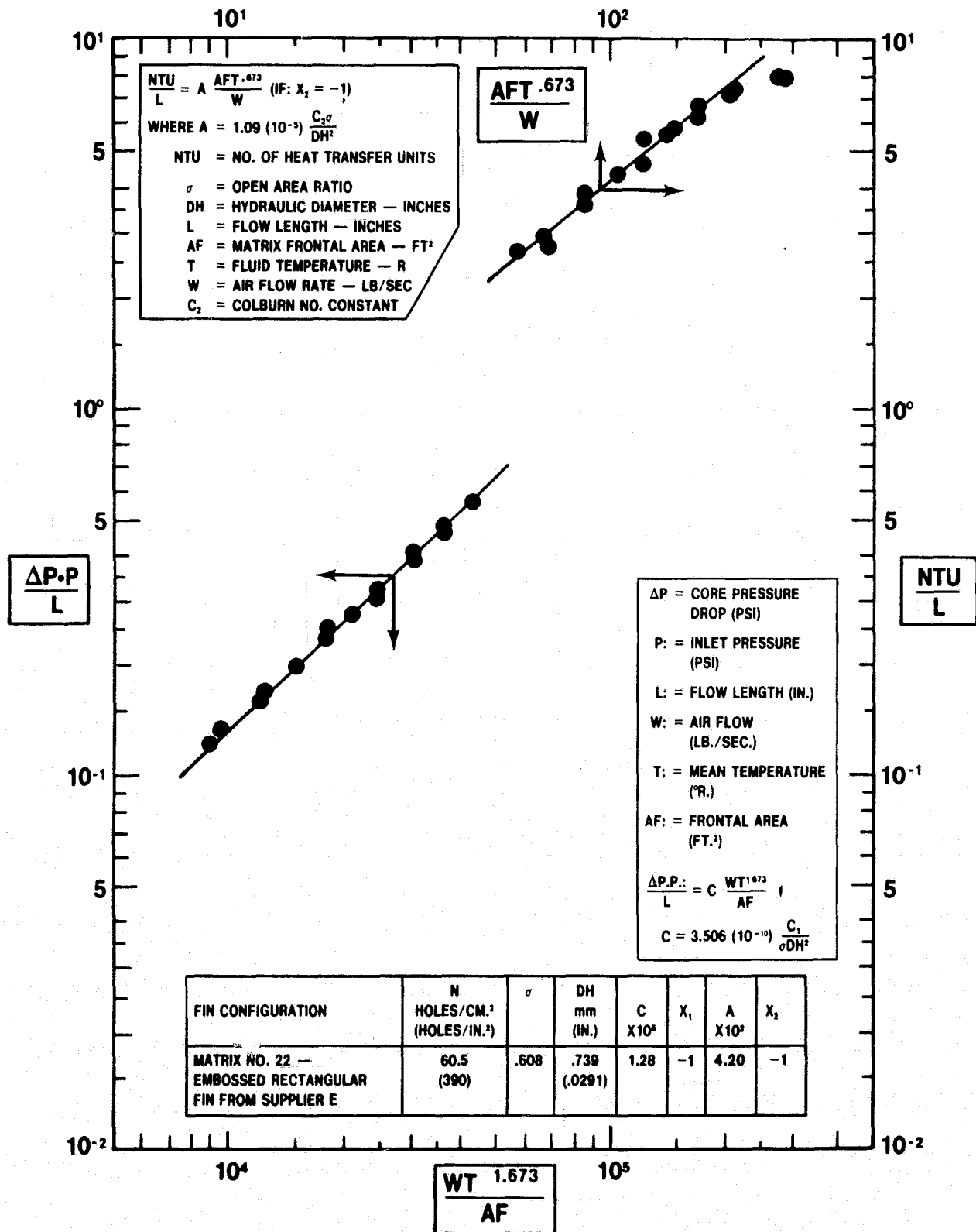


Figure IV.B.1.11 Alternate Thermodynamic Performance Characteristics of Matrix No. 22

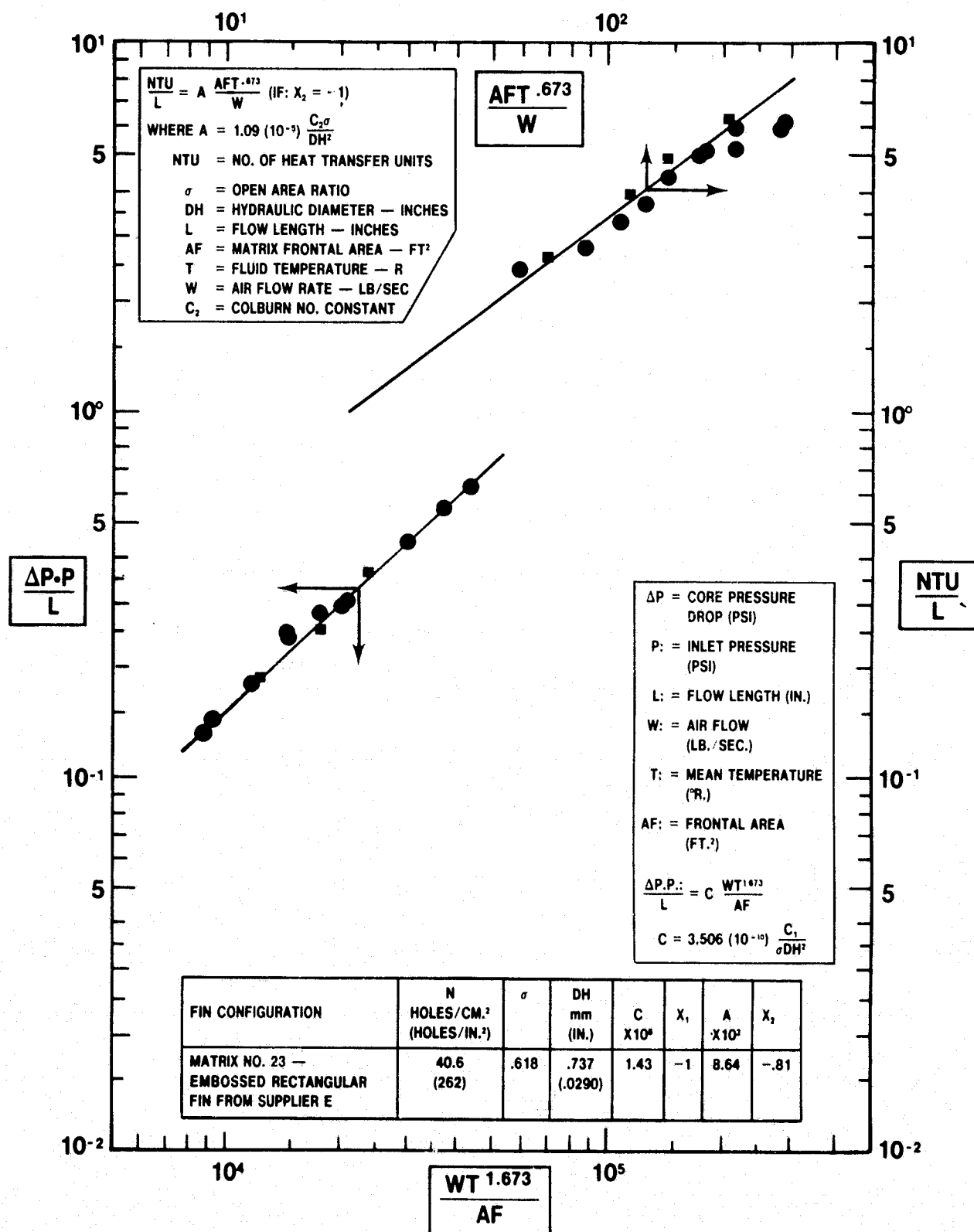


Figure IV.B.1.12 Alternate Thermodynamic Performance Characteristics of Matrix No. 23

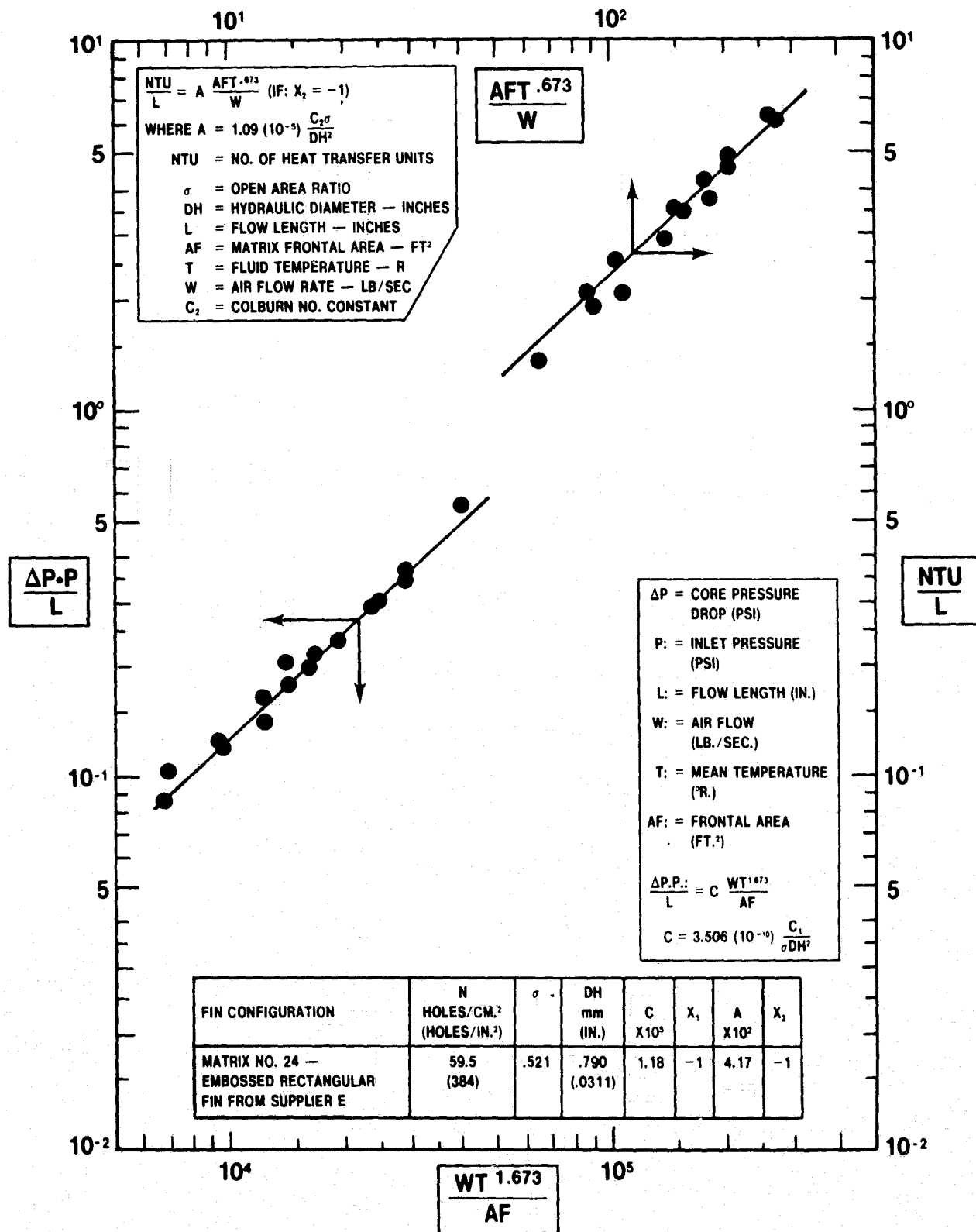


Figure IV.B.1.13 Alternate Thermodynamic Performance Characteristics of Matrix No. 24

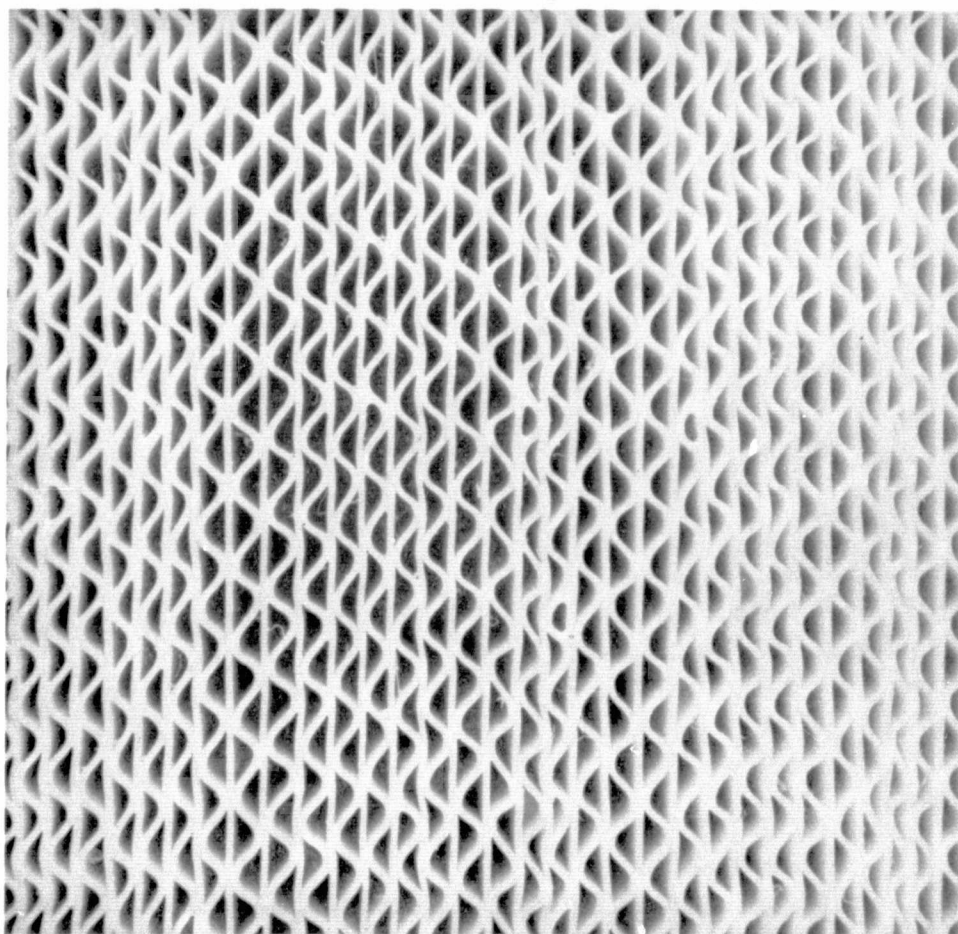


Figure IV.B.1.14 Photograph of Matrix No. 25

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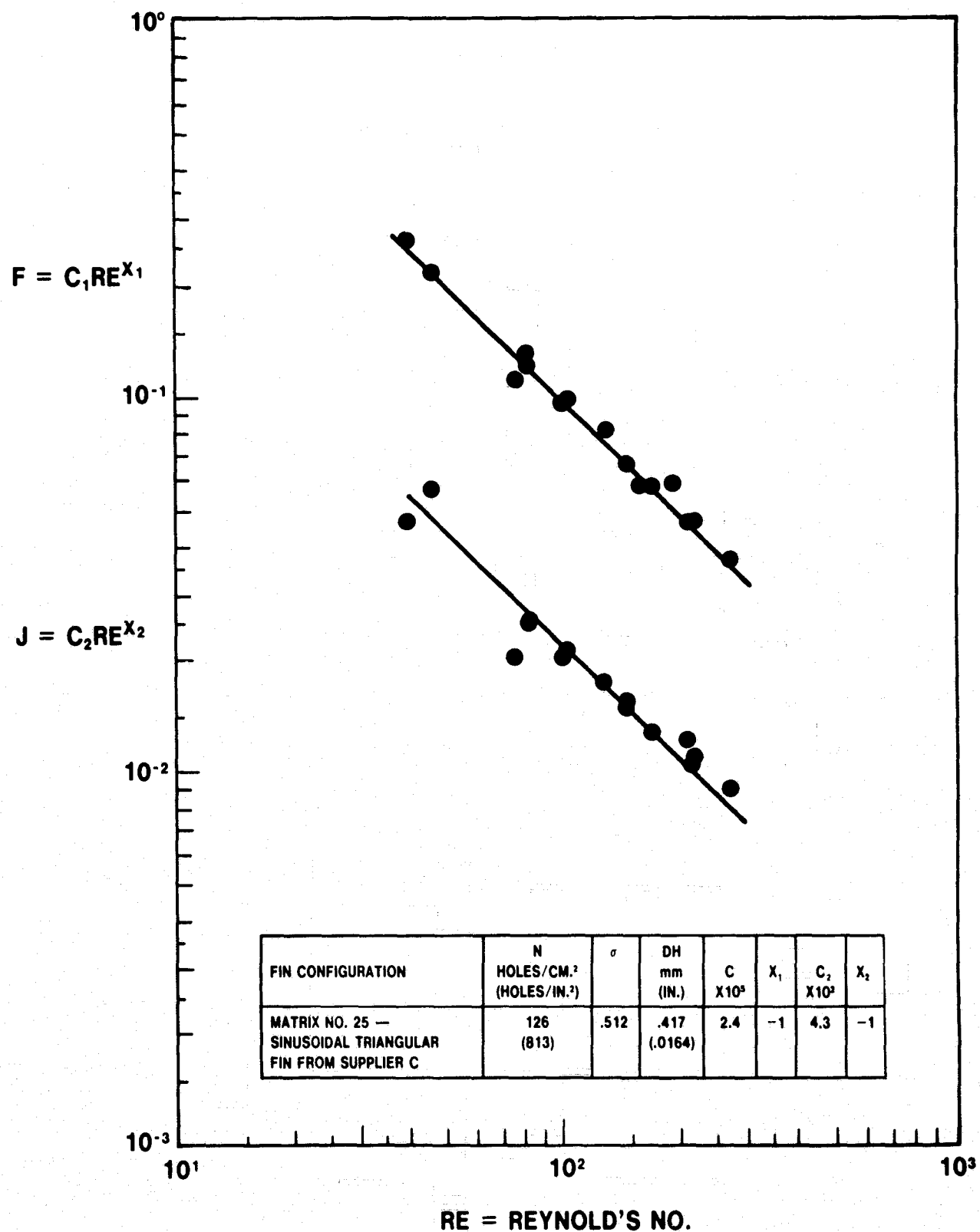
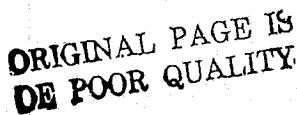


Figure IV.B.1.15 Standard Thermodynamic Performance Characteristics of Matrix No. 25



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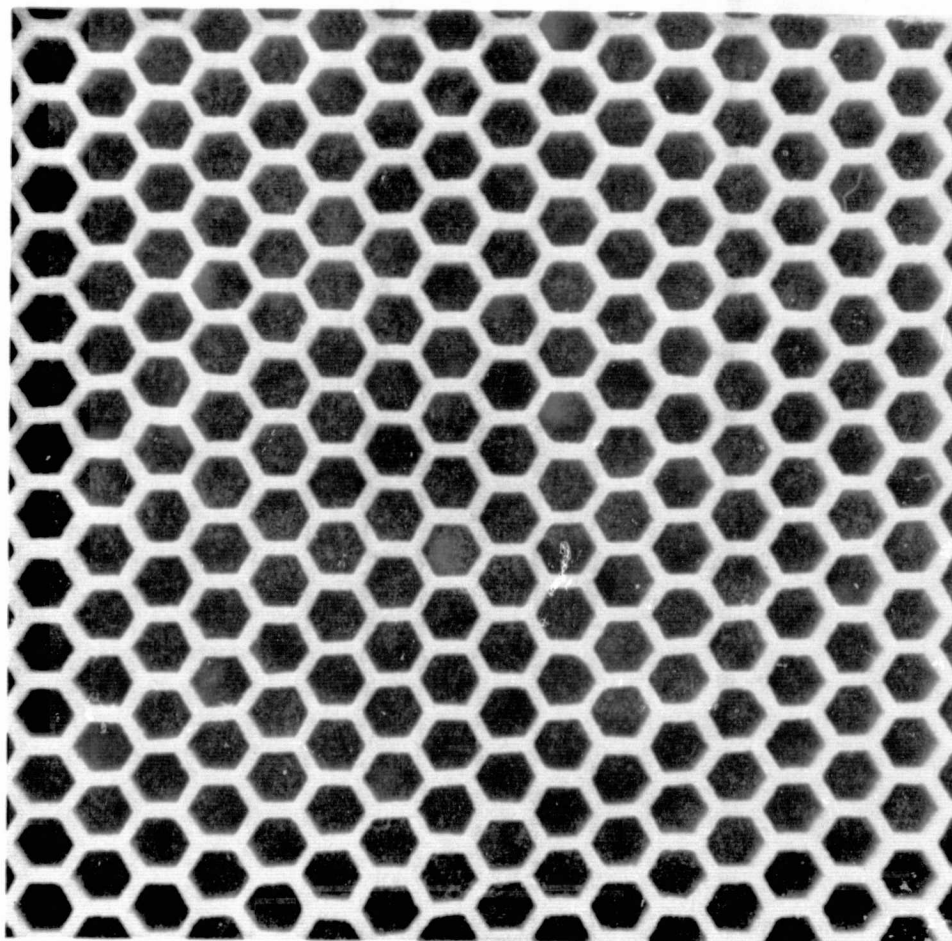


Figure IV.B.1.17 Photograph of Matrix No. 26

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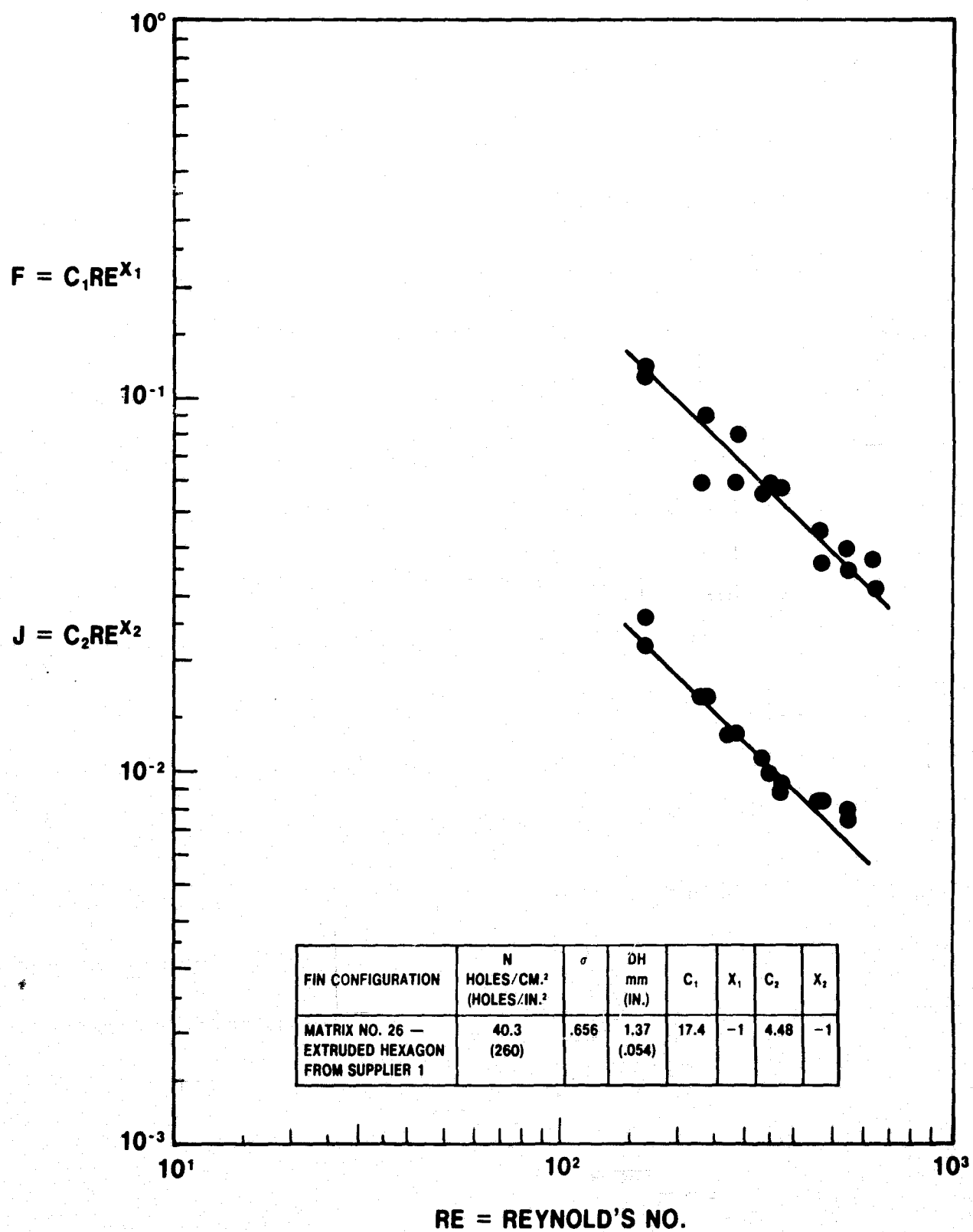


Figure IV.B.1.18 Standard Thermodynamic Performance Characteristics of Matrix No. 26

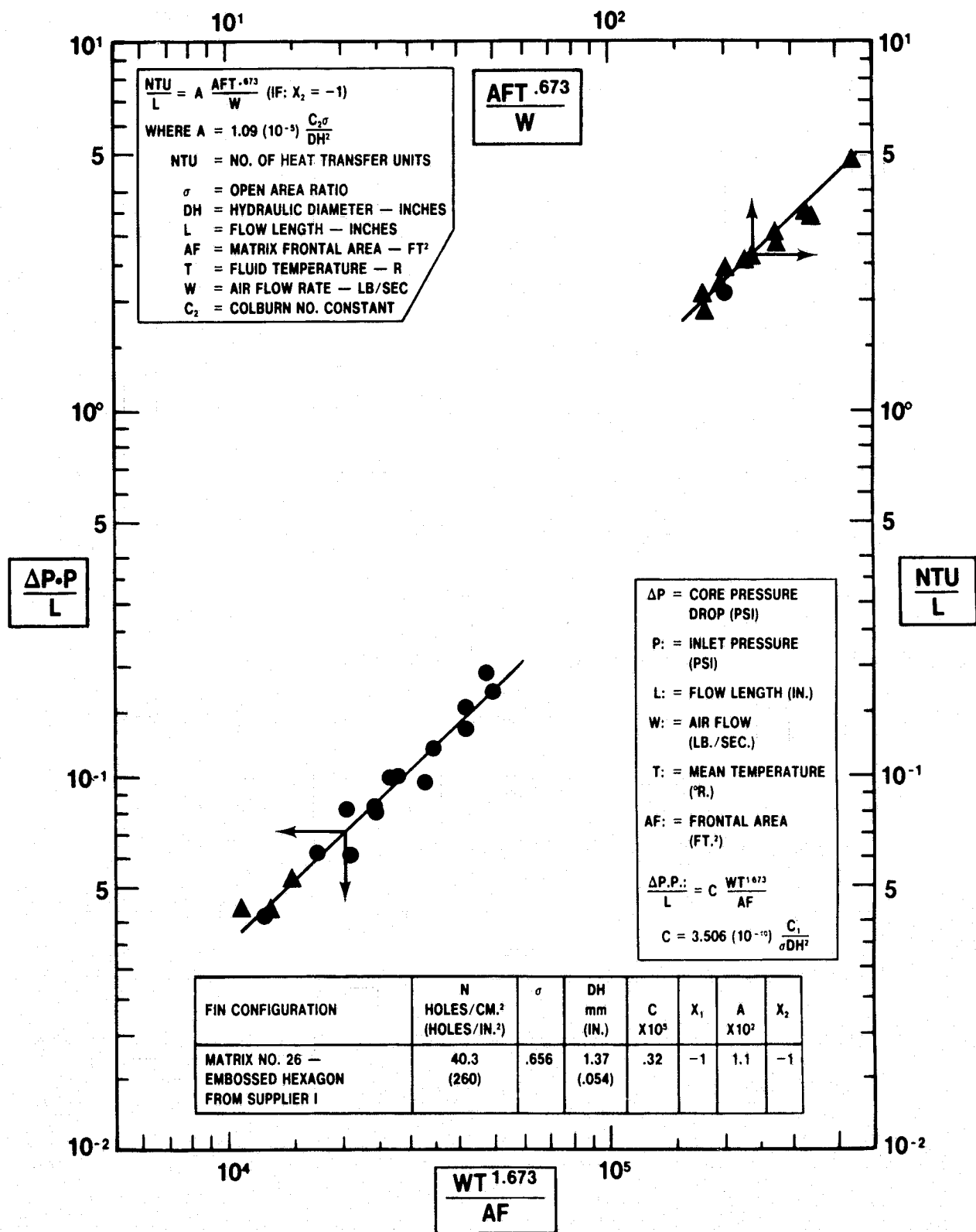


Figure IV.B.1.19 Alternate Thermodynamic Performance Characteristics of Matrix No. 26

IV.B.2 The Effect of Manufacturing Process on Fin Efficiency

One of the theoretical idealizations of the transient technique utilized by the shuttle rig method of evaluation is that the flow entering the matrix is steady and uniform in velocity and temperature. A flow imbalance can occur within the matrix test sample when fin geometry variance exists. Consequently, this condition can substantially alter the heat transfer characteristics and, to a lesser extent, the pressure drop characteristics (Reference 4). Hence, the shuttle rig provides a true evaluation of the performance characteristics for a particular matrix geometry with respect to shape uniformity and dimensional tolerance limitations inherent in the applicable manufacturing process. Based on the existing matrix sample size, the corrugating, extrusion and calendering fabrication techniques can be evaluated.

Variations in roll pressure during forming, tension during wrapping, and shrinkage rate during firing can induce significant fin shape variance for both the corrugating and calendering processes as shown on Figure IV.B.2.1. In addition, a dispersal of circumferential delaminations (Figure IV.B.2.1) can occur due to variations in bond quality during the wrapping phase of the process.

Although the extrusion process is susceptible to variations in material thickness due to die wear, it has the best potential for fabricating uniform fin shape. Similar to the wrapped processes, the extrusion technique (which inherently eliminates delaminations due to bonding) is susceptible to variations in shrinkage during the firing cycle.

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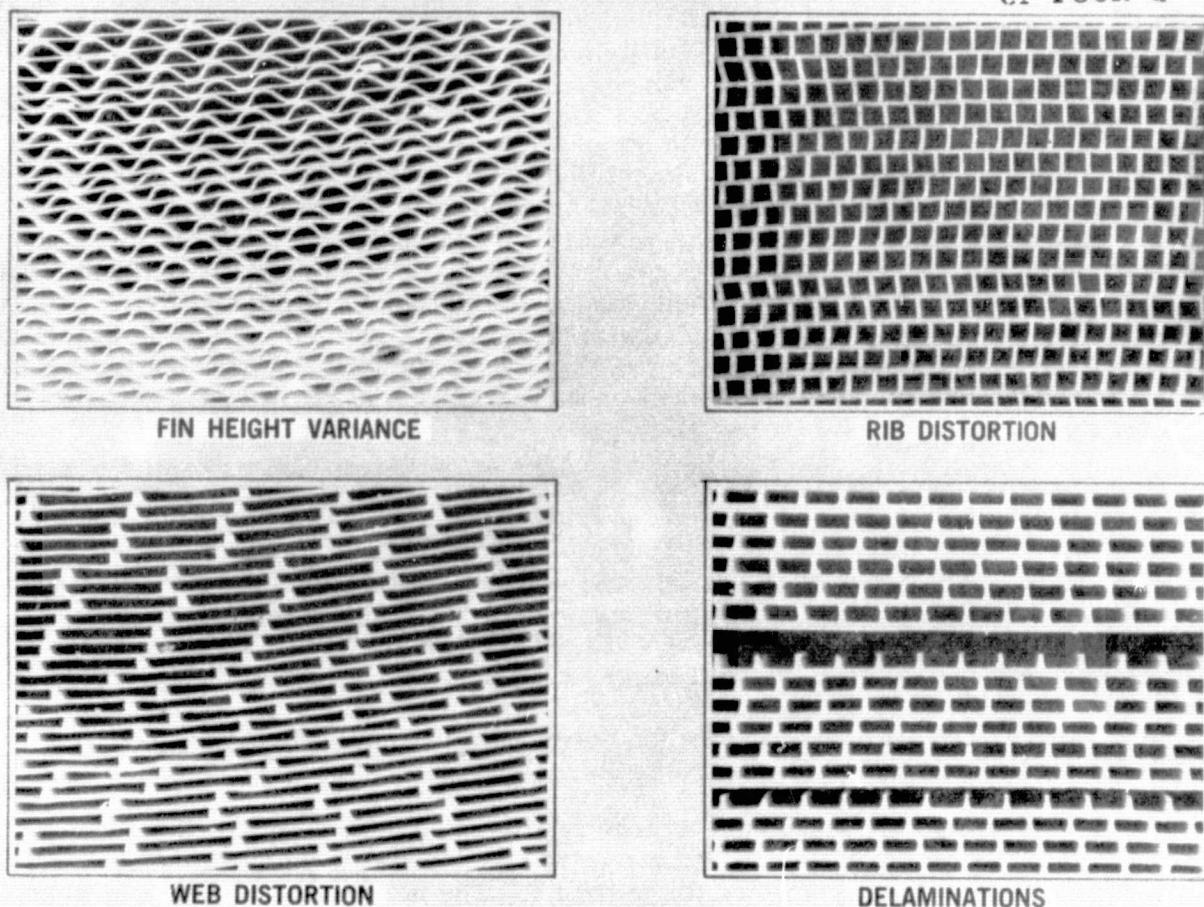


Figure IV.B.2.1 Photographs Illustrating Manufacturing Defects

In addition, the condition of the flow passage surface perpendicular to the air flow can also influence the performance characteristics. As the surface becomes rougher, the flow friction and heat transfer characteristics are both likely to increase. It is difficult to ascertain which parameter will increase at a faster rate without an evaluation in a shuttle rig or equivalent test facility. Obviously, it would be desirable to increase the heat transfer characteristics at a faster rate which would increase the overall fin efficiency (J/F).

In order to illustrate the importance of matrix sample quality, similar fin geometries with equivalent hydraulic diameters from the existing sample size (Tables IV.B.1.1 and IV.B.1.2) can be selected. These samples will be compared based on the standard heat transfer (J) and pressure drop (F) characteristics, which are based on the actual geometrical opening with the wall material thickness factored out. For optimum engine performance it is desirable to maximize the heat transfer efficiency and minimize the pressure loss associated with the heat exchanger. Therefore, the effect of matrix sample quality can be estimated by comparing the ratio of the heat transfer (J) to pressure drop (F) characteristics for similar fin geometries.

For the square flow passage the extruded matrix no. 12 can be compared to similar embossed matrices 6 and 16 (Table IV.B.2.1). The uniformity of the extruded configuration indicates improved heat transfer characteristics with equivalent pressure drop characteristics. The net result is an improvement of approximately 20% in overall fin efficiency (J/F).

To evaluate the effect of matrix sample quality for sinusoidal triangular structures, the pertinent fin parameters for three different examples are listed on Table IV.B.2.1. Matrices no. 10 and 19 were corrugated by Supplier A from the same tooling. Matrix No. 19, which had improved cell geometry uniformity, indicates a 20% improvement in the heat transfer characteristics ($J = C_2 RE^{X2}$) with equivalent pressure drop characteristics ($F = C_1 RE^{X1}$).

Matrix No. 20 was corrugated by Supplier A with a much thinner material using the same tooling that was designed for the thicker material of matrix no. 1. Consequently, the radius of curvature (R) for matrix 20 was increased during the forming process, which reduced the stiffness of the structure. With the reduced stiffness, the variation in fin height and shape was accentuated during the wrapping and firing stages of the process for matrix no. 20. The results indicate a significant increase in the pressure drop characteristics with a slight decrease in the heat transfer characteristics. The net result produced an apparent reduction of 22% in overall fin efficiency (J/F) for matrix 20.

This example illustrates the difficulty in comparing performance characteristics of sinusoidal triangular fins with equivalent aspect ratios (PH/H) and hydraulic diameters (DH). Unless the radius of curvature (R) of the structures are also equivalent, the actual shape of the fin configurations can be significantly different. In order to illustrate this, figure IV.B.2.2 depicts sinusoidal triangular configurations with constant aspect ratio and three different radii. This figure clearly illustrates the extreme variance in fin shape that can occur, depending on the radius of curvature (R) induced from the corrugating, wrapping and firing processes. In addition, the degree of blockage in the corners from the bonding process can introduce another significant variable that can affect performance characteristics.

The third example for sinusoidal triangular fins compares matrices 5 and 25 (Table IV.B.2.1) from Supplier C. Matrix 25 was fabricated from a new set of tooling similar to the tooling utilized for matrix 5. The new tooling produced a more uniform structure, which resulted in a significant improvement in overall fin efficiency (J/F).

CODE:

R — RECTANGULAR
 I.T. — ISOSCELES TRIANGULAR
 S.T. — SINUSOIDAL TRIANGULAR
 H — HEXAGONAL

$$F = C_1 REX_1$$

$$J = C_2 REX_2$$

$$\frac{\Delta P.P}{L} = C$$

$$\frac{WT}{AF} = 1.673$$

$$\frac{NTU}{L} = A \left[\frac{AFT}{W} 0.673 \right]^{-X_2}$$

CORE NO.	SUP-PLIER	TYPE OF FIN	X FINS CM.	Y ROWS CM.	N HOLES CM.2	S MM. (IN.)	B MM. (IN.)	H MM. (IN.)	PH MM. (IN.)	A.R. PH H	P _w E CC.	L DH	σ	DH MM. (IN.)	β M.2 M.3	C x 10 ⁵	A x 10 ²	C ₁	X ₁	C ₂	X ₂	J/F RE = 100
			(FINS IN.)	(ROWS IN.)	(HOLES IN.2)						(LB FT.3)				(FT.2 FT.3)							
12	A	R	7.0 (17.8)	7.1 (18)	49.6 (320)	.290 (.0114)	.290 (.0114)	1.123 (.0442)	1.427 (.0562)	1.27	1.67 (104.2)	62	634	1.130 (.0445)	209 (684)	.46	1.75	16.5	-1	5.0	-1	.303
6	D	R	6.9 (17.5)	7.5 (19)	51.5 (332)	.300 (.0118)	.300 (.0118)	1.036 (.0408)	1.450 (.0571)	1.40	1.47 (91.4)	67	615	1.092 (.0430)	210 (687)	.515	1.55	16.7	-1	4.28	-1	.256
16	D	R	6.6 (16.8)	6.7 (17.0)	44.2 (286)	.315 (.0124)	.315 (.0124)	1.179 (.0464)	1.511 (.0595)	1.28	1.51 (94.2)	60	625	1.186 (.0467)	196 (642)	.44	1.30	17.1	-1	4.16	-1	.243

10	A	S.T.	14.6 (37)	14.4 (36.5)	209.5 (1351)	.066 (.0026)	.066 (.0026)	.630 (.0248)	1.374 (.0541)	2.18	2.09 (130.4)	128	.767	.549 (.0216)	520 (1707)	1.60	5.9	16.3	-1	3.29	-1	.202
19	A	S.T.	15.0 (38)	13.6 (34.5)	203.9 (1311)	.061 (.0024)	.061 (.0024)	.676 (.0266)	1.336 (.0526)	1.98	2.10 (131.0)	122	.787	.579 (.0228)	504 (1653)	1.26	6.50	16.1		3.94	-1	.245

1	A	S.T.	11.0 (28)	13.0 (33)	143 (924)	.109 (.0043)	.109 (.0043)	.660 (.0260)	1.813 (.0714)	2.75	2.20 (137.0)	121	.673	.589 (.0232)	424 (1392)	1.30	4.55	13.4	-1	3.34	-1	.249
20	A	S.T.	10.8 (27.5)	14.6 (37)	157.4 (1018)	.066 (.0026)	.066 (.0026)	.620 (.0244)	1.846 (.0727)	2.98	2.18 (136.3)	107	.780	.592 (.0233)	489 (1605)	1.36	5.00	16.4	-1	3.19	-1	.195

5	C	S.T.	9.9 (25)	15.4 (39)	151.1 (975)	.135 (.0053)	.135 (.0053)	.51 (.020)	2.0 (.080)	3.95	1.43 (89.0)	179	.569	.401 (.0158)	530 (1734)	2.25	10.2	9.1	-1	.62	-.80	.171
25	C	S.T.	9.9 (25)	12.8 (32.5)	126 (813)	.183 (.0072)	.183 (.0072)	.61 (.024)	2.0 (.080)	3.39	2.33 (145.7)	153	.512	.417 (.0164)	457 (1496)	2.40	4.3	9.4	-1	2.07	-1	.220

Table IV.B.2.1 Effect of Matrix Sample Quality on Shuttle Rig Results

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Based on the examples listed in Table IV.B.2.1, fin shape uniformity can result in a 20 to 25% improvement in overall fin efficiency (J/F) for square and sinusoidal structures with equivalent aspect ratio (PH/H) and hydraulic diameters (DH).

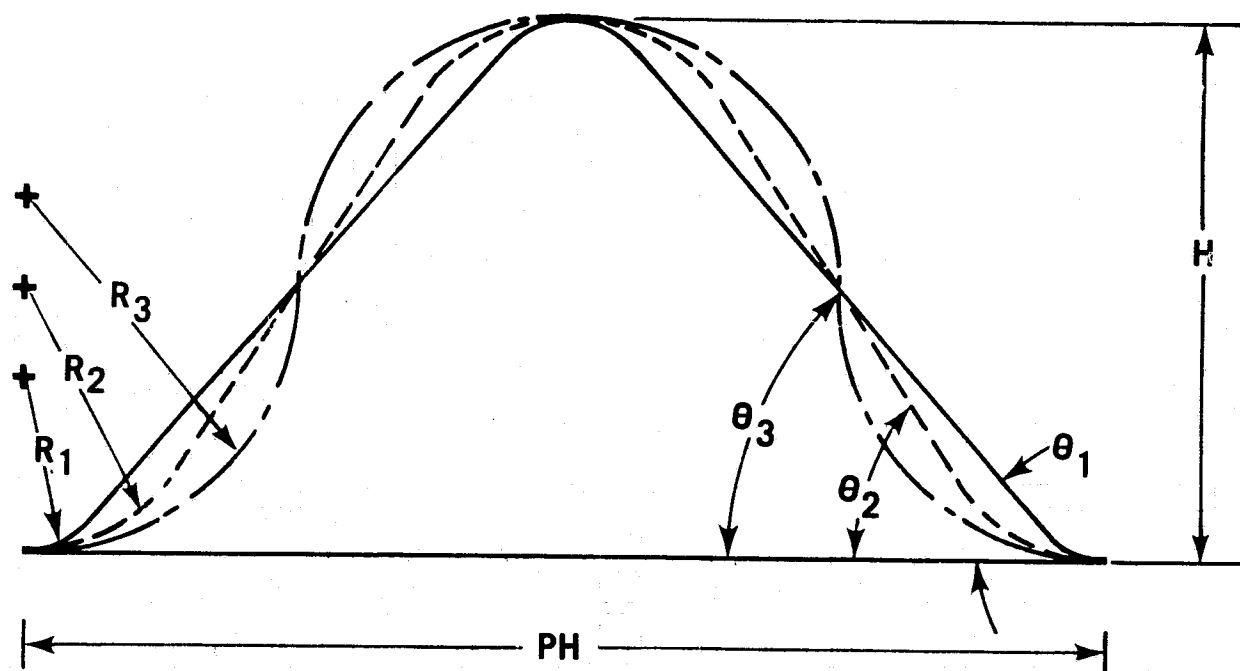


Figure IV.B.2.2 Schematic Illustrating Variation in Sinusoidal Triangular Fin Passage Shape with Respect to Radius of Curvature for a Constant Aspect Ratio

IV.B.3 The Effect of Matrix Fin Geometry on Performance

Based on the shuttle rig test data presented in section IV.B.1, the effect of matrix fin shape on aerothermodynamic performance can be discussed. In order to evaluate the effect a change in matrix fin shape and/or package size will have on regenerator effectiveness and pressure drop, simplified parametric curves can be utilized. Regenerator pressure drop and effectiveness can be expressed in terms of flow conditions, package size and matrix fin parameters as follows (Reference 5):

$$U_o \text{ \& } (\Delta P/P) = \phi \left[(\text{FLOW CONDITIONS}) \cdot (\text{PACKAGE SIZE}) \cdot (\text{FIN PARAMETERS}) \right]$$

$$U_o = \left[\frac{2\mu}{W.P.R^{2/3}} \right] \cdot \left[L(A_F) \right] \cdot \left[\frac{C_2 \sigma}{DH^2} \right] \quad (\text{IF: } X_2 = -1)$$

$$\frac{\Delta P}{P} = \left[\frac{2\mu WRT}{g_C P^2} \right] \cdot \left[\frac{L}{A_F} \right] \cdot \left[\frac{C_1}{\sigma DH^2} \right]$$

$$\epsilon = \frac{U_o}{U_o + 1} \quad (\text{IF: } X^* = 1)$$

Where:

X_2 = Reynold's No. exponent for Colburn No. ($J = C_2 RE^{X_2}$)

C_1 = Fanning Friction Factor Constant for Laminar Flow ($F = C_1/RE$)

C_2 = Colburn No. Constant for Laminar Flow

σ = Open Area Ratio

DH = Hydraulic Diameter

W = Air Flow Rate

μ = Viscosity

R = Universal Gas Constant

T = Average Temperature

g_C = Proportionality Factor in Newton's Second Law

L = Flow Length

A_F = Frontal Area

ΔP = Pressure Drop

P = Inlet Pressure

ϵ = Effectiveness = $\phi(U_0, X^*)$

X^* = Flow Capacity Rate Ratio

U_0 = Number of Heat Transfer Units (NTU)

PR = Prandtl Number

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Utilizing these expressions for a constant set of regenerator flow conditions, the parametric curves shown on figure IV.B.3.1 were generated. In order to utilize these curves, the effectiveness and pressure drop must be determined for the reference or baseline regenerator size and fin geometry at a given set of flow conditions. Once the reference regenerator size and fin geometry have been selected, the following expressions must be determined for each change in fin geometry or package size:

C^* = Ratio of Fanning Friction Factor constant with respect to reference laminar flow Fanning Friction Factor constant, C_1/C_{1B}

C^{**} = Ratio of Colburn No. constant with respect to reference laminar flow Colburn No. constant, C_2/C_{2B}

L^* = Flow length or thickness ratio with respect to reference regenerator thickness, L/L_B

DH^* = Hydraulic diameter ratio with respect to reference fin hydraulic diameter, DH/DH_B

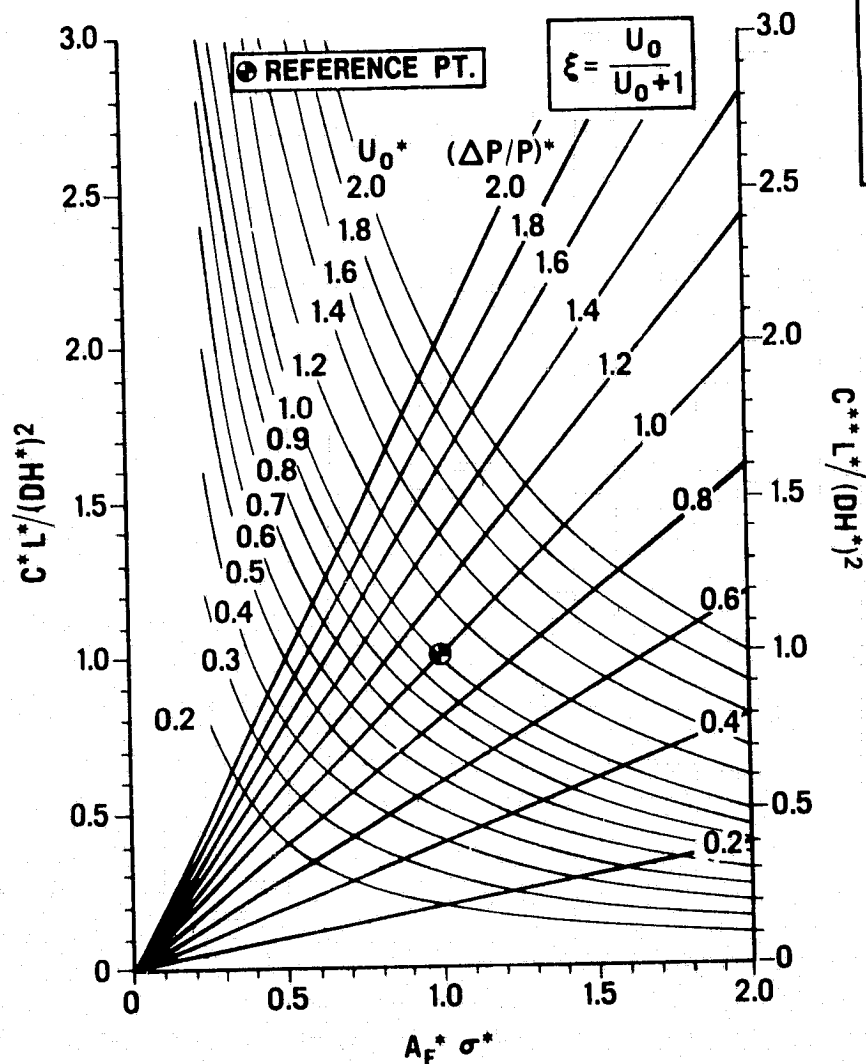
A_F^* = Frontal area ratio with respect to reference regenerator frontal area, A_F/A_{FB}

σ^* = Ratio of open area percentage with respect to reference fin open area ratio, σ/σ_B

U_o^* = Number of heat transfer units ratio with respect to reference regenerator number, U_o/U_{oB}

$(\Delta P/P)^*$ = Pressure drop ratio with respect to reference regenerator pressure drop, $(\Delta P/P)/(\Delta P/P)_B$

After the above ratios are determined, the effect of alterations in fin geometry and/or package size can be estimated with respect to the reference or baseline configuration performance, provided the regenerator flow conditions are not altered.



$C^* = C_1/C_{1B}$
 $C^{**} = C_2/C_{2B}$
 $L^* = L/L_B$
 $DH^* = DH/DH_B$
 $A_F^* = A_F/A_{FB}$
 $\sigma^* = \sigma/\sigma_B$
 $U_o^* = U_o/U_{oB}$
 $(\Delta P/P)^* = (\Delta P/P)/(\Delta P/P)_B$
 SUBSCRIPT B = REFERENCE VALUE

Figure IV.B.3.1 Parametric Curves for Heat Exchanger Thermodynamic Performance

Keeping in mind the considerations discussed for matrix sample quality, a comparison of performance characteristics between the rectangular, square, hexagonal, sinusoidal and isosceles triangular passages can be made. In order to compare matrix fin configurations on an equivalent basis, the hydraulic diameter, wall thickness and flow length should be approximately the same. The parametric curves (Figure IV.B.3.1) for performance were utilized to compare the effect of matrix fin shape.

From the present matrix sample size (Table IV.B.1.1 and IV.B.1.2) matrices 1 (sinusoidal), 2 (square), 4 (isosceles) and 9 (rectangular) have approximately the same hydraulic diameter and flow length. The standard and alternate heat transfer and pressure drop characteristics for these matrices are repeated on Figures IV.B.3.2 and IV.B.3.3, respectively. Since matrix 1 represents the fin geometry that was utilized for the Ford 707 production turbine engine, it will be selected as the reference configuration. The performance for the production size regenerator at full power conditions is designated as Case A in Table IV.B.3.1. The predicted performance of matrices 2, 4 and 9 for the same size regenerator are listed as Case B, C and D respectively in Table IV.B.3.1.

As expected, the extruded isosceles triangular matrix (4), which had higher heat transfer and lower pressure drop characteristics (Figure IV.B.3.2), resulted in higher effectiveness and lower pressure drop compared to the wrapped corrugated triangular matrix (1). Conversely, the rectangular (9) and square (2) matrices resulted in lower effectiveness than the reference matrix (1) in spite of the superior heat transfer characteristics (Figure IV.3.2). This discrepancy illustrates the importance of material thickness.

The reason for this discrepancy is attributed to the severe penalty imposed on the rectangular and square configurations by the significantly greater material thickness (S and B) which creates a significantly lower open area ratio (σ). Consequently, the alternate heat transfer characteristics (Figure IV.B.3.3) allow a direct comparison of existing fin configurations for a fixed regenerator size at identical flow conditions with the material wall thickness factored in. The standard thermodynamic performance characteristics (Figure IV.3.2) are based on the actual opening of the fin configuration with the material wall thickness factored out.

A better comparison of the square, rectangular and sinusoidal configurations could be made if the material thicknesses were equivalent. To maintain the same F and J characteristics for the square and rectangular surfaces, assuming they could be fabricated with reduced material thickness, the geometrical opening (A_{Δ}) is kept constant to maintain the same hydraulic diameter by adjusting the cell density (N) proportionately to the material thickness change. Case E and F in Table IV.B.3.1 illustrates the improvement in performance of the square and rectangular passages if the material thickness could be equivalent to that of the sinusoidal.

A similar comparison can be made between matrix 11 (sinusoidal) and rectangular matrices 8 and 24, since they have equivalent hydraulic diameters. For this example, matrix 11 will be selected as the reference fin with the predicted performance at full power in the Ford 707 production turbine engine listed as case G in Table IV.B.3.1. Rectangular matrices 8 and 24 listed as case H and I, respectively, show higher effectiveness combined with a significant increase in pressure drop compared to the sinusoidal matrix (case G). Similar to the first example, rectangular matrices 8 and 24 were hypothetically modified to have material thickness equivalent to the reference sinusoidal matrix (11). The predicted performance for the modified rectangular matrices 8 and 24 are listed in Table IV.B.3.1 as case J and K, respectively. As expected the difference in effectiveness is even greater with respect to the sinusoidal matrix (case G) with only a slight penalty for pressure drop.

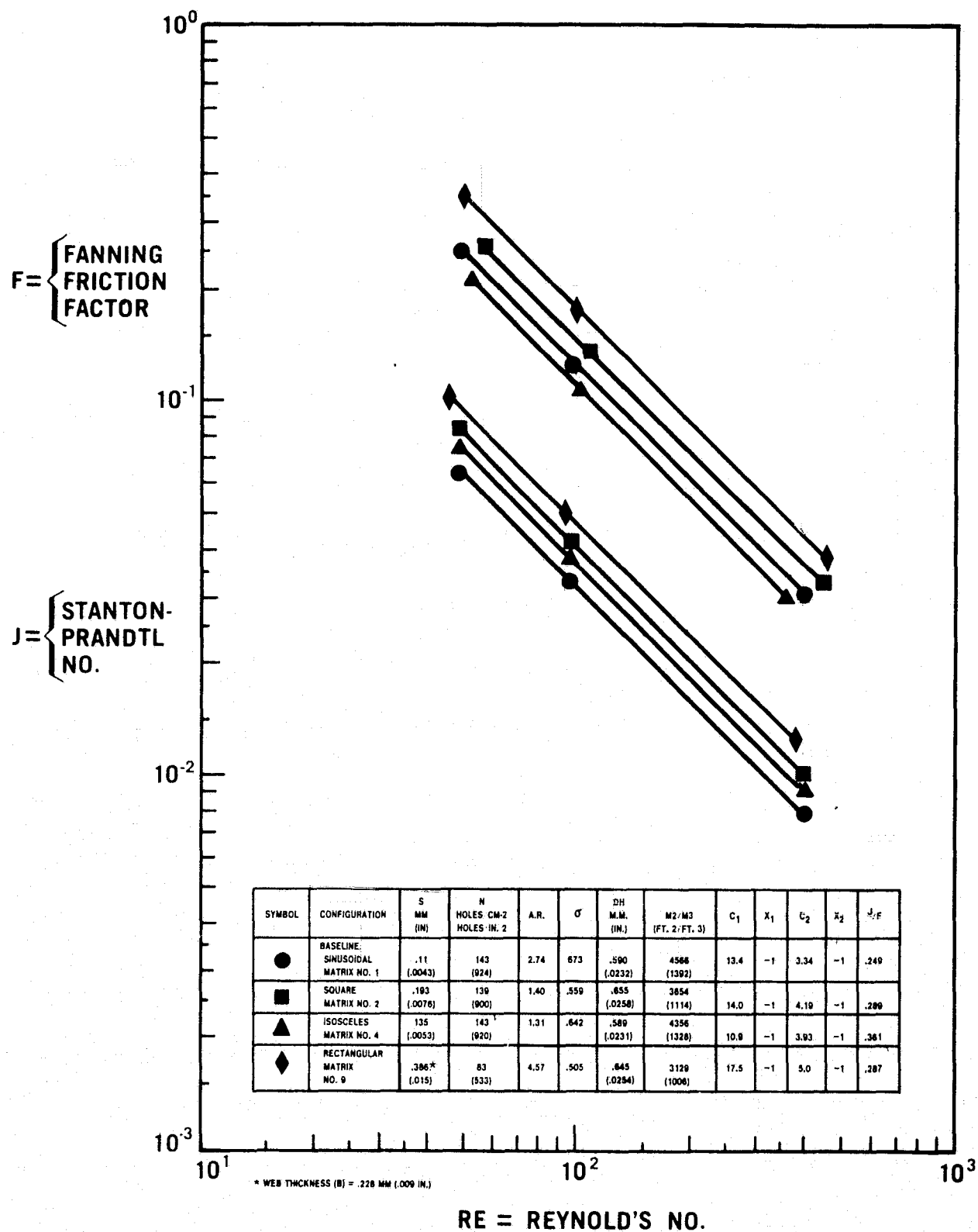


Figure IV.B.3.2 Standard Thermodynamic Performance Characteristics for Matrices 1, 2, 4 and 9.

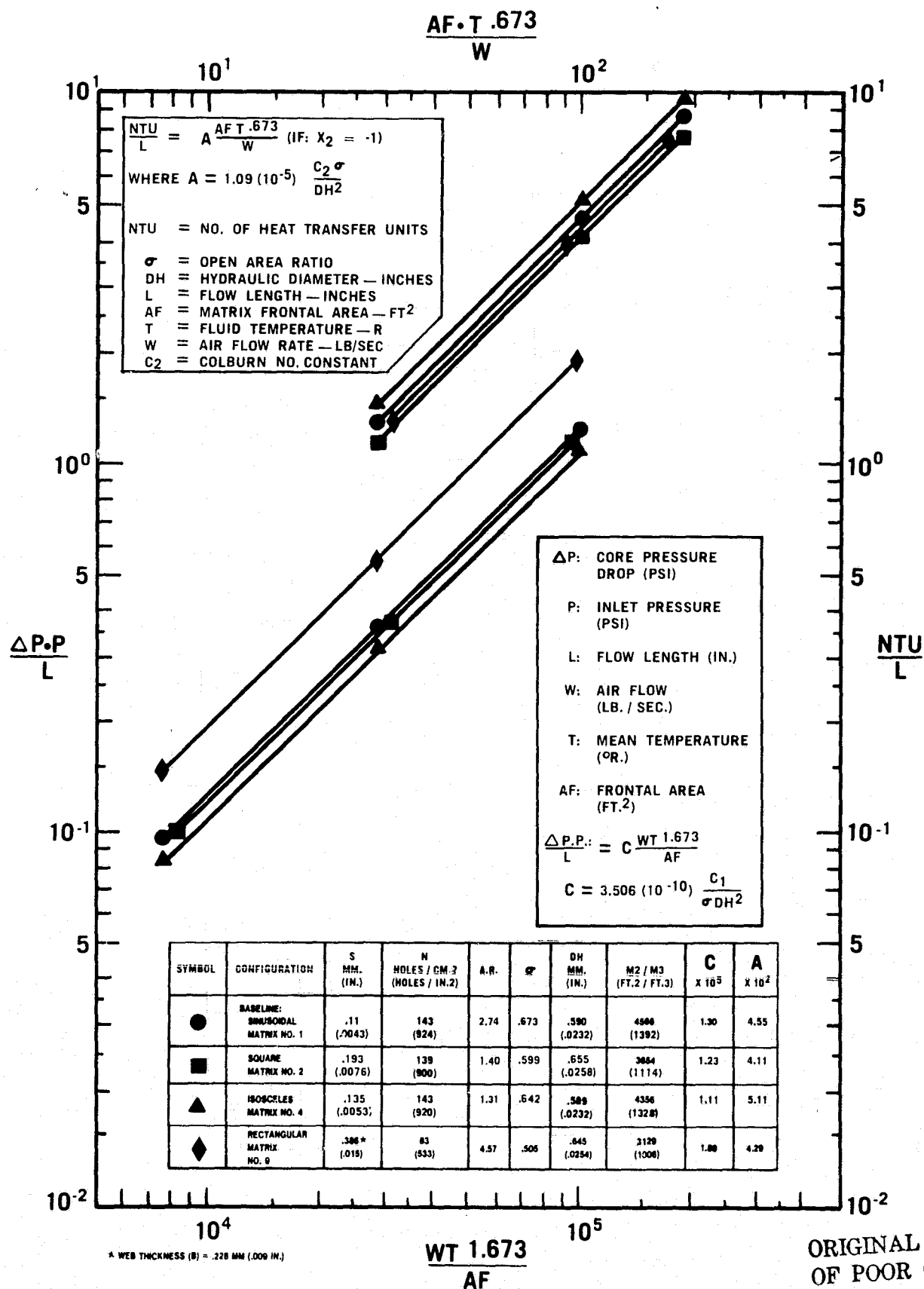


Figure IV.B.3.3 Alternate Thermodynamic Performance Characteristics for Matrices 1, 2, 4 and 9.

CODE:

R — RECTANGULAR

SQ. — SQUARE

S.T. — SINUSOIDAL TRIANGULAR

I.T. — ISOSCELES TRIANGULAR

$$F = C_1 RE^{-1}$$

$$J = C_2 RE^{-1} \quad \epsilon = \frac{U_0}{U_0 + 1}$$

CASE NO.	MATRIX NO.	TYPE OF FIN	A _F FT. ²	L IN.	VOL IN. ³	S IN.	B IN.	σ	DH IN.	AR	C ₁	C ₂	A _F σ^*	$\frac{C^*L}{(DH)^2}$	$\frac{C^{**}L}{(DH)^2}$	($\Delta P/P$) [*]	u ₀ [*]	u ₀	ϵ %	($\Delta P/P$) _T %
A	1	S.T.	3.58	2.82	1454	.0043	.0043	.673	.0232	2.75	13.4	3.34	1	1	1	1	1	6.70	87.0	3.93
B	2	SQ.				.0076	.0076	.599	.0258	1.40	14.0	4.19	.89	.84	1.01	.95	.88	5.90	85.5	3.73
C	4	I.T.				.0053	.0053	.642	.0232	1.31	10.9	3.93	.95	.81	1.18	.87	1.13	7.57	88.3	3.42
D	9	R				.0152	.0090	.505	.0254	4.57	17.5	5.0	.75	1.09	1.25	1.50	.93	6.23	86.2	5.90
E	2 ‡	SQ.				.0043	.0043	.735	.0258	1.27	14.0	4.19	1.09	.84	1.01	.75	1.10	7.37	88.1	2.95
F	9 ‡	R				.0043	.0043	.736	.0254	3.90	17.5	5.0	1.09	1.09	1.25	1.0	1.35	9.05	90.0	3.93
G	11	S.T.	3.58	2.82	1454	.0050	.0050	.720	.0305	3.73	14.5	2.79	1	1	1	1	1	3.52	77.9	2.45
H	8	R				.0112	.0084	.611	.0310	5.0	18.4	5.55	.85	1.23	1.91	1.45	1.60	5.63	84.9	3.55
I	24	R				.0164	.0110	.521	.0311	4.10	16.9	4.17	.72	1.12	1.44	1.58	1.00	3.52	77.9	3.87
J	8 ‡	R				.0050	.0050	.750	.0310	4.67	18.4	5.55	1.04	1.23	1.91	1.18	1.95	6.26	87.3	2.89
K	24 ‡	R				.0050	.0050	.746	.0311	3.54	16.9	4.17	1.04	1.12	1.44	1.08	1.45	5.10	83.6	2.65

‡ — EXISTING MATRIX MODIFIED TO HAVE THE IDENTICAL OPENING WITH REDUCED MATERIAL THICKNESS (S AND B).

Table IV.B.3.1 Comparison of Effectiveness and Pressure Drop Characteristics

The primary reason for the improved performance of the square and rectangular geometries compared to a sinusoidal structure with equivalent material thickness is attributed to a reduction in boundary layer effects in the corners. In addition, the calendered rectangular fin does not have the double wall regions inherent in the wrapped corrugation process. Consequently, blockage is minimized and the available heat transfer surface area is utilized more effectively.

Since the first example (Case A thru F in Table IV.B.3.1) did not clearly define the relative merits of the extruded isosceles triangular structure with respect to the embossed square and rectangular surfaces, a second comparison can be made. Selection of the square (matrix 12) and isosceles (matrix 18) extrusions, which have equivalent hydraulic diameter and flow length, eliminates the effect of manufacturing process on performance. Based on the standard performance characteristics for these matrices, which are repeated on Figure IV.B.3.4, the difference in overall fin efficiency (J/F) is negligible for Reynold's No. greater than 200.

Based on the two sets of data presented, the square surface appears to be slightly better than the isosceles triangular configuration provided the manufacturing process is equivalent. No conclusion can be drawn with respect to embossed rectangular structures due to their dependence on matrix quality and the influence of aspect ratio, which will be discussed in the next section.

The relative merits of a square and hexagonal extrusions can be compared by utilizing matrices 13 and 26, respectively. The standard performance characteristics for these matrices are repeated on Figure IV.B.3.5. Due to the difference in slope of the heat transfer characteristics, the comparison is dependent on the flow range (RE) selected. Based on this limited data, no conclusion can be reached as to the relative merits of the square and hexagonal configurations until the sample size is increased.

The examples in this section have served to illustrate the important factors that must be considered when selecting an optimum fin configuration for a given heat exchanger application. When selecting the best existing fin geometry for a given heat exchanger size and flow conditions, the passage geometry, material thickness and limitations of the method of fabrication must all be considered. Utilization of the alternate heat transfer (A) and pressure drop (C) characteristics inherently accounts for all of these factors for matrices fabricated from existing tooling.

When selecting the most efficient fin shape for a new set of tooling, the standard heat transfer (J) and pressure drop (F) characteristics can be utilized, since the material thickness is factored out. Once the desired fin geometry is selected the fabrication technique must be capable of producing the required material thickness and sample quality. This explains how an apparently less efficient fin shape (sinusoidal) can yield improved performance (case A thru D in Table IV.B.3.1) for a specific regenerator application due to the ability of the fabrication method to produce a much thinner structure.

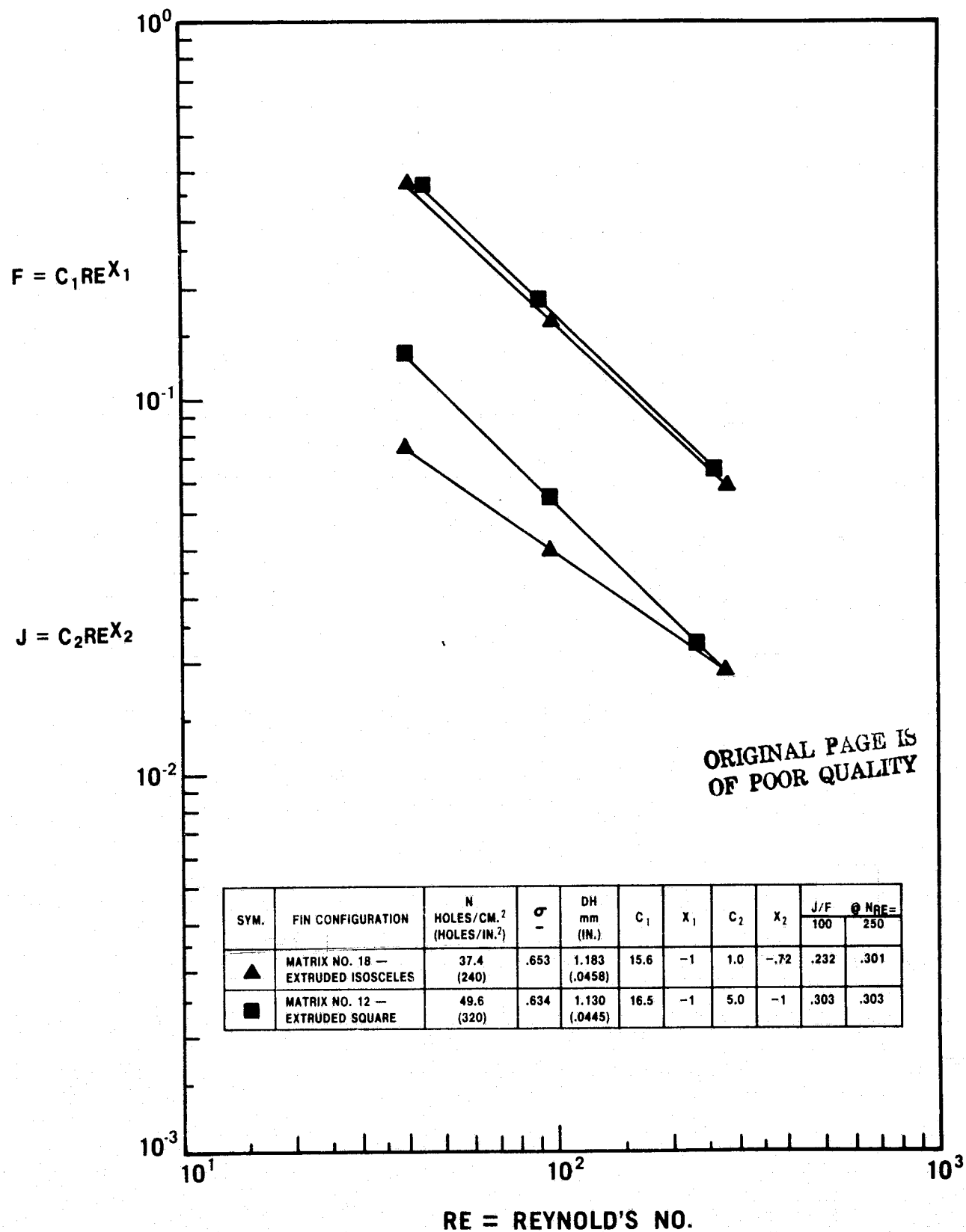


Figure IV.B.3.4 Standard Thermodynamic Performance Characteristics for Matrices 12 and 18.

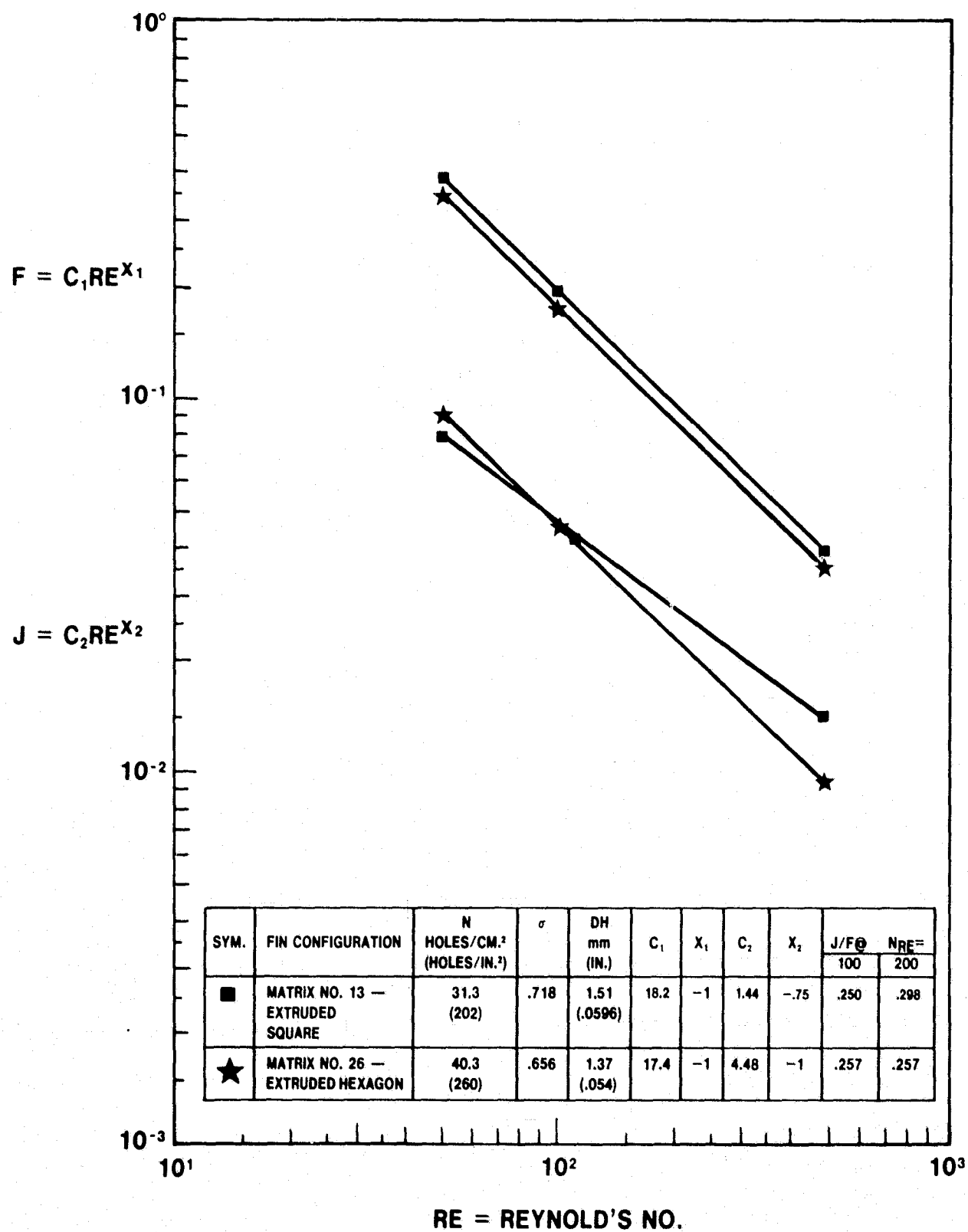


Figure IV.B.3.5 Standard Thermodynamic Performance Characteristics for Matrices 13 and 26

IV.B.4 Influence of Aspect Ratio

Theoretical solutions for laminar flow, forced convection, heat transfer and flow friction available in the literature (Reference 6) are dependent on the accuracy of definition of the geometry and a limited number of boundary conditions that are not well duplicated in actual practice or even in the laboratory. In addition, the theoretical solutions apply only to fully developed laminar flow, which requires the length to hydraulic diameter ratio (L/DH) be greater than 100. Depending on the degree of variation in the matrix fin shape uniformity, length to hydraulic diameter ratio and actual thermal boundary condition imposed by the application environment, the disparity between computed and actual performance characteristics can be significant.

With the shuttle rig technique, limitations inherent in the applicable manufacturing process that influence matrix fin shape and uniformity are automatically accounted for. In addition, deviations from fully developed laminar flow can also be evaluated. Therefore, design predictions based on performance characteristics determined from shuttle rig test data will minimize the disparity between computed and actual heat exchanger performance.

Keeping in mind the considerations discussed above and utilizing the test data accumulated at this time on rectangular and sinusoidal triangular surfaces, the effect of aspect ratio and length to hydraulic diameter on performance characteristics can be investigated.

For the rectangular fin configuration, aspect ratio (A.R.) is defined as the ratio of the distance between rib centerlines (PH) to the rib height (H), as shown on Figure IV.A.4. In effect this definition specifies the groove spacing and depth from the embossing roll after firing shrinkage has occurred. Theoretical predictions for performance characteristics are based on the aspect ratio of the actual opening ($PH-S/H$). In order to evaluate the rectangular configurations on an equivalent basis with theory, the effect of aspect ratio of the actual opening ($PH-S/H$) will be evaluated. For the present rectangular fin sample size (15 matrices) the modified aspect ratio ($PH-S/H$) variance is 1.0 to 9.1 while the length to hydraulic diameter ratio varies from 47 to 109. To evaluate the effect of aspect ratio on performance, only the matrices approaching fully developed laminar flow will be considered ($L/DH > 90$). Theoretically the slope (X_2) of the Colburn No. (J) characteristics should be -1 for fully developed laminar flow. Depending on matrix sample quality and actual laminar flow condition (L/DH), the slope of the heat transfer characteristic can be less than -1. Since 33% of the rectangular samples evaluated have a slope less than -1, a comparison of heat transfer characteristics will depend on the Reynold's No. range selected. Based on the predominant operating range for the Ford 707 gas turbine engine, the Reynold's No. range selected for discussion will be 100 to 200. The normal Colburn No. Constant (C_2) for laminar flow can be modified as follows:

$$J = C_2 RE^{X_2} \quad \text{or}$$

$$C_2 = \frac{J}{RE^{X_2}}$$

$$\text{Let: } C_2' = J_{RE} \cdot RE$$

Where: J_{RE} = value of Colburn No. at a given Reynold's No.

$$\text{If: } X_2 = -1; \text{ then } C_2' = C_2$$

The effect of aspect ratio on performance characteristics for rectangular matrices approaching fully developed laminar flow is shown on Figure IV.B.4.1. The pressure drop characteristics agree very well with theoretical predictions. As expected, within the limits of sample quality, the heat transfer characteristics appear to improve with

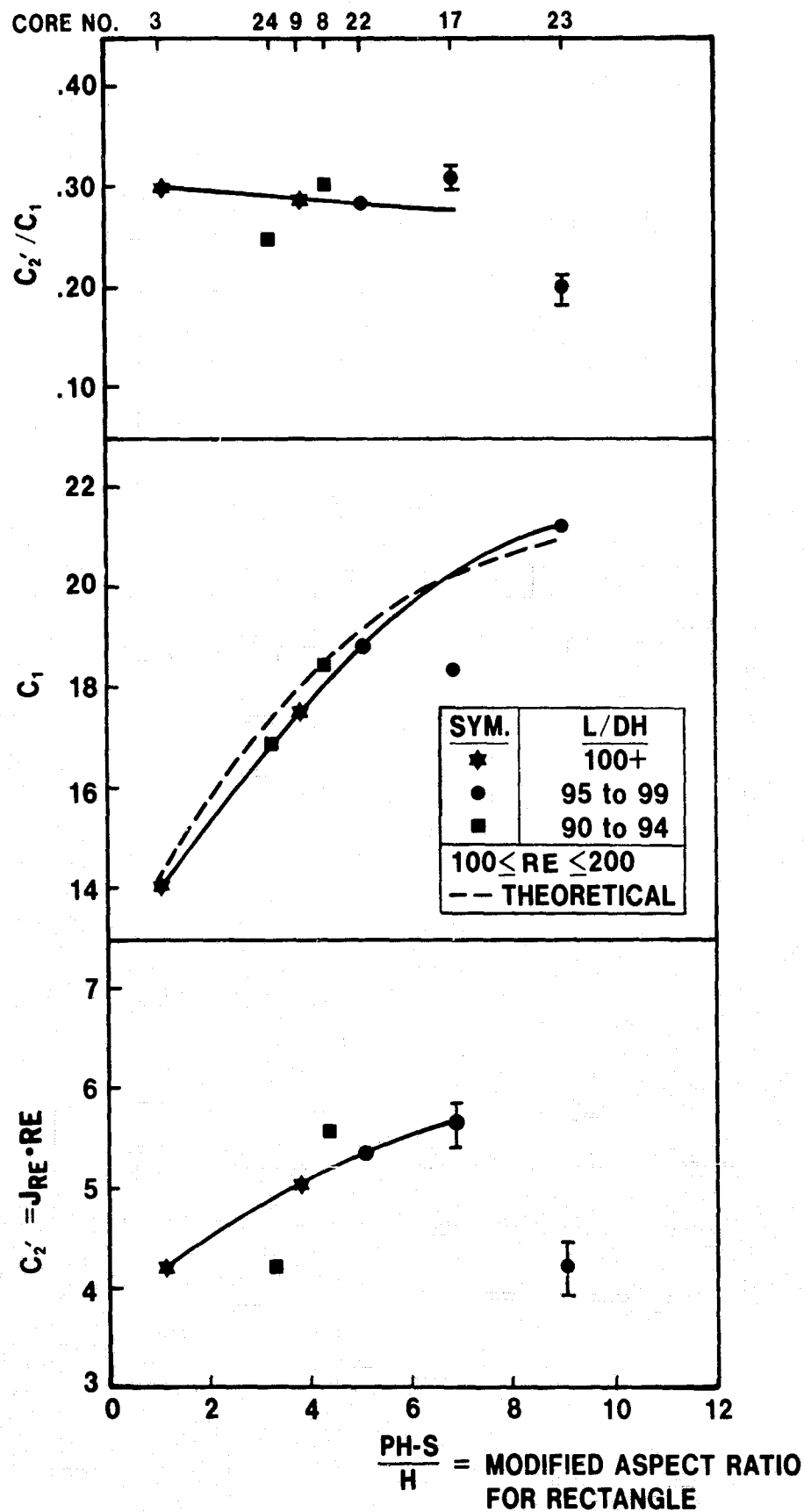


Figure IV.B.4.1 Performance Characteristics for Rectangular Fins vs. Aspect Ratio

increasing aspect ratio up to a value of 7. When the aspect ratio exceeds a value of 7, the expected improvement appears to be offset by excessive cell geometry distortion. This is not surprising due to the susceptibility of the high aspect ratio rectangular fins to back web distortion (Figure IV.B.2.1). Since the heat transfer characteristics increase at a lower rate than the pressure drop characteristics, overall fin efficiency ($J/F = C_2'/C_1$) appears to decrease slightly with increasing aspect ratio.

To eliminate the influence of fin shape for the evaluation of the effect of L/DH on performance characteristics, rectangular configurations that are essentially square (modified aspect ratio, $PH-S/H < 1.1$) were selected (Figure IV.B.4.2). Since the pressure drop characteristics decrease at a faster rate than the heat transfer characteristics, the net effect is an increase in overall fin efficiency (J/F) approaching fully developed laminar flow ($L/DH > 100$).

For a similar evaluation of sinusoidal triangular passages the aspect ratio must be modified in a different manner as shown on Figure IV.B.4.3 to attain an equivalent basis with theory for the actual opening. Performance characteristics of the sinusoidal matrices that approach fully developed laminar flow are shown on Figure IV.B.4.4 with respect to the modified aspect ratio $[(PH-2K)/(H-S)]$. Although the pressure drop characteristics exhibit the same general trend as the theory, the correlation is not good. Unlike the rectangular surfaces the heat transfer characteristics diminish with increasing aspect ratio. Similar to the rectangular passage, the net result is that overall fin efficiency appears to decrease with increasing aspect ratio.

The scatter in the pressure drop data substantiates the difficulty in comparing performance characteristics of sinusoidal configurations on the basis of aspect ratio, unless the radii of curvature (R) of the corrugations are equivalent. The significance of the radius of curvature was previously discussed in Section IV.B.2 and illustrated on Figure IV.B.2.2.

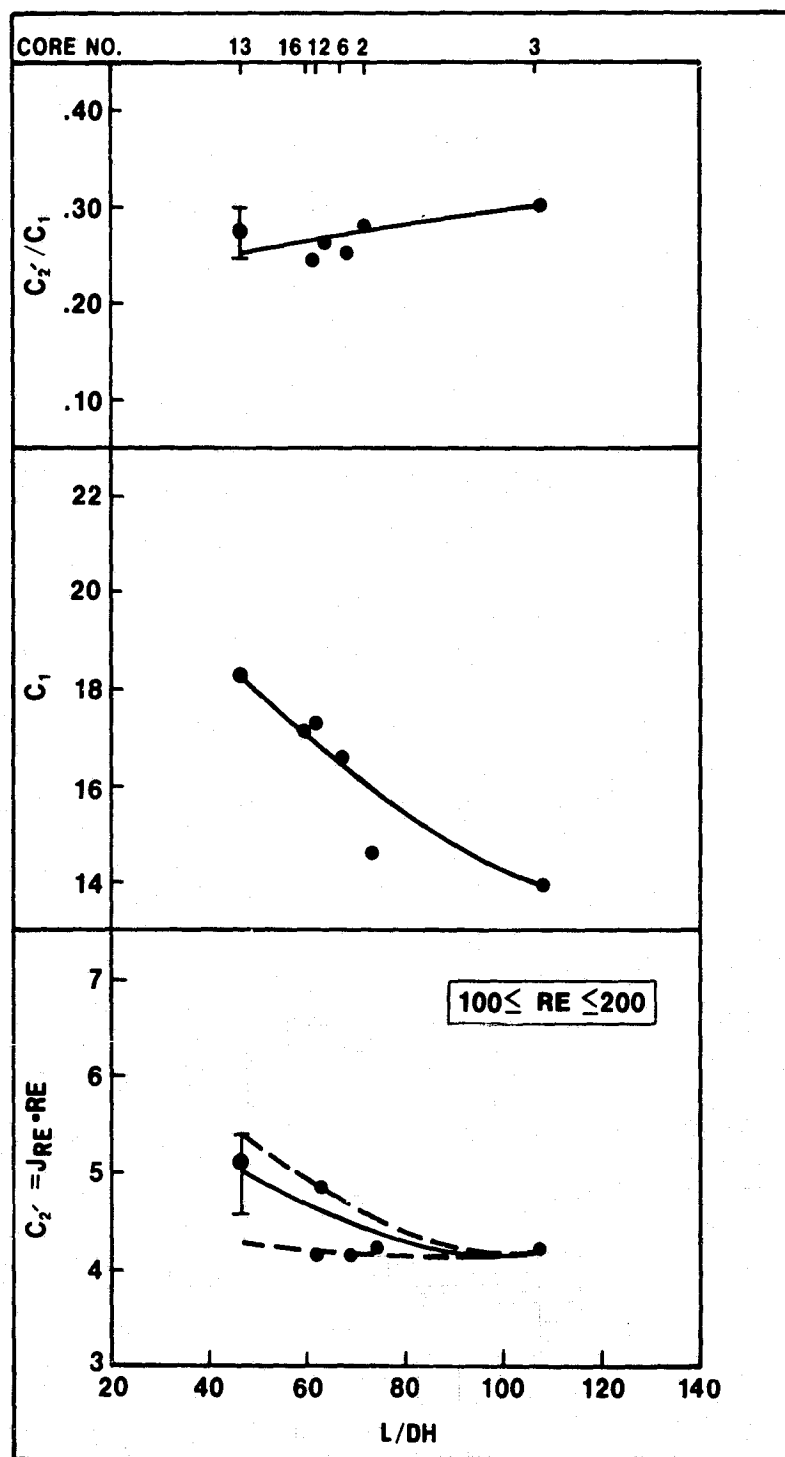
Unlike the rectangular fin sample size, the present group of sinusoidal surfaces is devoid of a large group of samples for a given aspect ratio with a large variance for length to hydraulic diameter ratio. Consequently, the existing sample size must be subdivided into three sub-groups in an attempt to attain a preliminary estimate of the influence of L/DH on performance (Figure IV.B.4.5). Similar to the square passage, within the limits of the expected increased scatter in the data, both the heat transfer and pressure drop characteristics appear to decrease with increasing L/DH . Unlike the square passage, the overall fin efficiency (J/F) does not appear to be influenced by the laminar flow regime. Additional samples are required before a firm conclusion can be made.

The data presented in this section substantiated the importance of manufacturing process considerations with respect to matrix fin geometries that was discussed in Section IV.B.3. Two additional factors that must be considered have also been illustrated.

When selecting a matrix fin geometry for a fixed size heat exchanger the effect of length to hydraulic diameter should be considered. If the L/DH of the particular application is significantly different than the matrix test sample flow conditions, the performance characteristics of the matrix fin geometry should be adjusted accordingly.

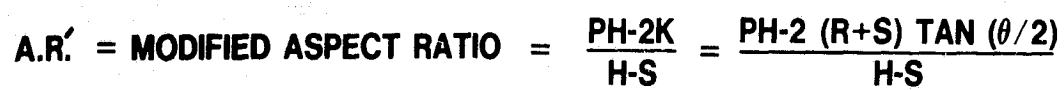
Another factor to be considered in the selection of the optimum matrix fin geometry for a particular application is the trade-off in engine performance with respect to heat exchanger effectiveness and pressure drop. In general, engine specific fuel consumption (SFC) and horsepower (HP) are influenced by heat exchanger effectiveness (ϵ) and pressure drop ($\Delta P/P$), respectively. If SFC is a more important factor for

a given engine application, then J/F considerations are not adequate for proper matrix geometry selection. For example, the heat transfer characteristics for the rectangular matrix geometry increase significantly with increasing aspect ratio (Figure IV.B.4.1), provided the matrix non-uniformity is minimized. At the same time the overall fin efficiency (J/F) decreases with increasing aspect ratio. Therefore, a rectangular fin with a high aspect ratio might be a better fin selection even though the overall fin efficiency (J/F) is reduced.



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Figure IV.B.4.2 Effect of Length to Hydraulic Diameter Ratio for Rectangular Fins



EQUIVALENT SINUSOIDAL PASSAGE

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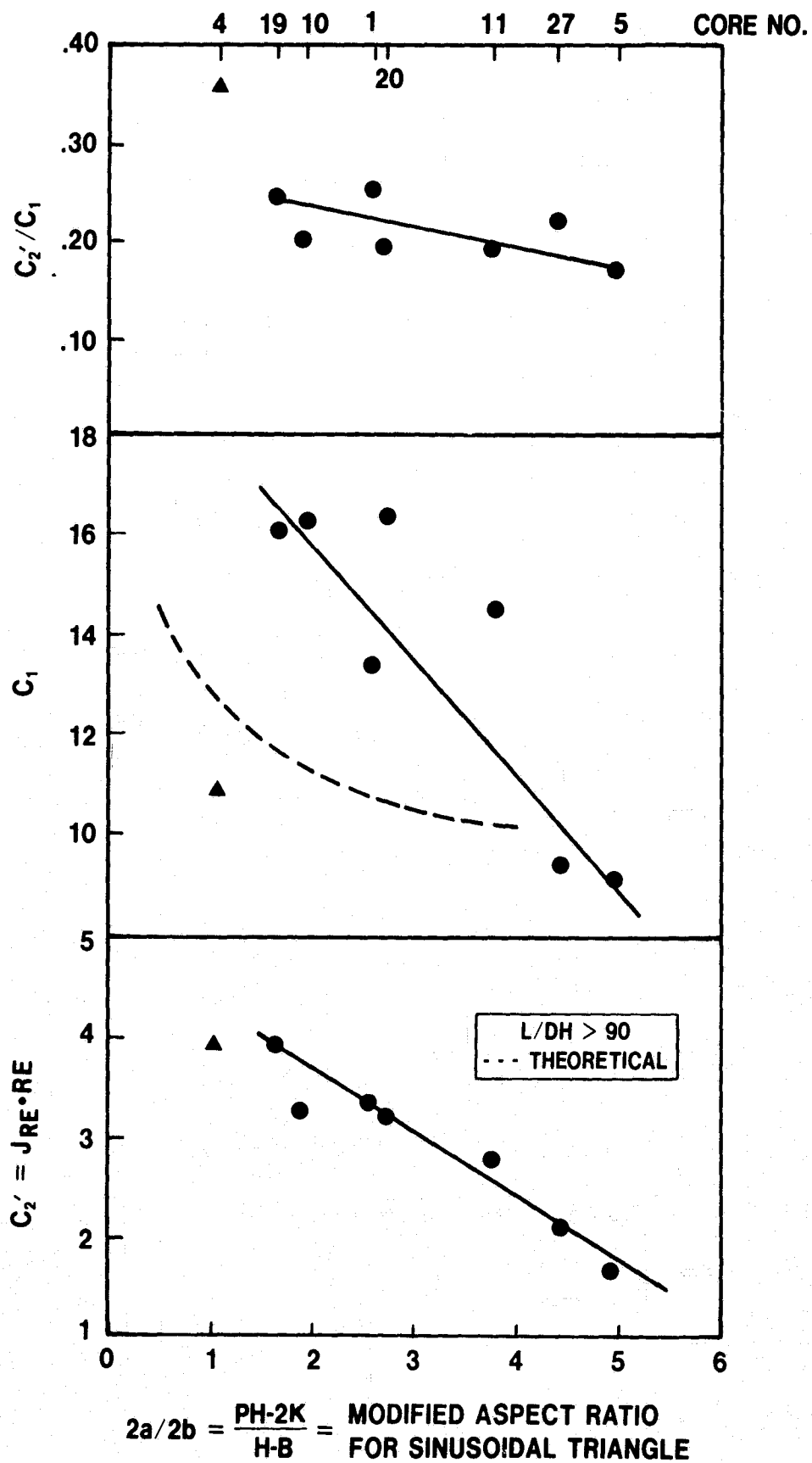


Figure IV.B.4.4 Performance Characteristics for Sinusoidal Triangular Fins vs. Aspect Ratio

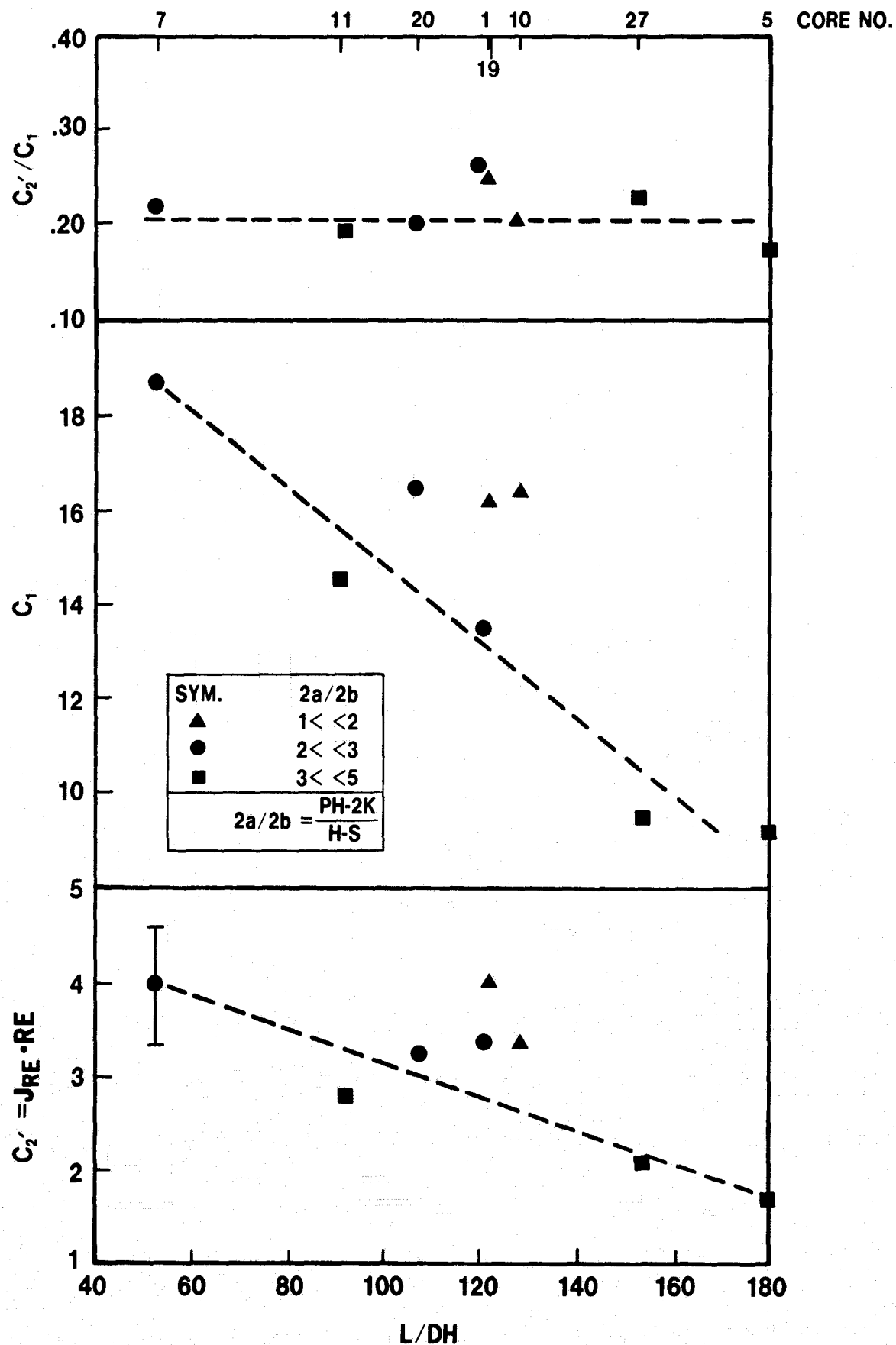


Figure IV.B.4.5 Effect of Length to Hydraulic Diameter Ratio For Sinusoidal Triangular Fins

IV. C. PROBLEM AREAS

There are no significant problems at this time.

IV.D. FUTURE PLANS

New matrix surface geometries will be evaluated as they become available from the various sources. As the matrix sample size increases, the observations and trends discussed in Section IV.B. will be supplemented where applicable.

IV.E. TASK SUMMARY

A total of twenty-six matrix fin configurations have been evaluated at the present time. Fifteen rectangular, eight sinusoidal, two isosceles triangular, and one hexagonal configuration comprise the present matrix sample size.

Several factors that merit design considerations in the utilization of this test data were illustrated as follows:

1. Matrix Quality
2. Fin Geometry
3. Aspect Ratio
4. Length to Hydraulic Diameter

Aerothermodynamic performance characteristics can be significantly altered by variations in fin shape uniformity, bond quality and surface roughness. Based on the present matrix sample size, up to 25% variation in overall fin efficiency can occur due to manufacturing process limitations.

Utilizing overall fin efficiency (J/F) as a basis for comparison, and considering matrix fin configurations with equivalent hydraulic diameter, material thickness, and flow length, the effect of fin geometry can be summarized as follows:

1. The sinusoidal triangular structure appears to be the least efficient fin configuration. An increase in heat transfer efficiency accompanied by an increase in pressure drop can be obtained by reducing the aspect ratio of this type of surface geometry to approach the extruded isosceles triangular structure.
2. The square passage geometry appears to be slightly better than the isosceles triangular structure provided the manufacturing process quality is equal.
3. For embossed rectangular structures, heat transfer and pressure drop characteristics appear to increase with increasing aspect ratio.
4. Additional test data is required before the hexagonal flow passage can be compared to the square and triangular structures.

The heat transfer and pressure drop characteristics for the square and sinusoidal configurations appear to decrease as the length to hydraulic diameter ratio (L/DH) approaches fully developed laminar flow ($L/DH > 100$).

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TASK V. DESIGN STUDIES OF ADVANCED REGENERATOR SYSTEMS

V. A. INTRODUCTION

Since 1973, design studies of ceramic heat exchanger systems have been carried out in order to analytically evaluate the thermal and structural performance of the various supplier's matrices and to compare different drive, mounting, seal and stress relief schemes. Two types of rotary heat exchangers have been studied: a regenerator sized for the Ford 707 gas turbine engine and a preheater sized for the Ford Stirling engine. The regenerator has a 710 mm (28.2 in) outside diameter and is 73 mm (2.86 in) thick. The preheater has a 460 mm (18.04 in) outside diameter, a 190 mm (7.50 in) inside diameter, and is 41 mm (1.6 in) thick. These ceramic heat exchanger systems have been analyzed for temperature inlet conditions of 800°C (1472°F) and 1000°C (1832°F) (Reference 1,2).

Task V of the NASA/Ford Ceramic Regenerator Program deals exclusively with gas turbine engine regenerator design studies emphasizing regenerator system materials and configurations intended to improve aerothermodynamic performance, reduce thermal stress, and provide for higher temperature operation.

V. B STATUS

V. B.1 Material Properties

In order to more accurately predict the structural performance of thin-wall aluminum silicate regenerators, a study has been undertaken to statistically characterize the strength of the thin-wall matrix.

Material specimens were obtained from a thin-wall aluminum silicate core which suffered a ring gear separation after 11 hours of engine operation and were used to determine the matrix modulus of rupture (MOR) in the radial and tangential directions and the radial compressive strength of the matrix. The MOR was determined by breaking samples in four-point bend tests. The compressive strength was determined by compressively loading specimens to failure in the radial direction. Because there is a considerable amount of scatter associated with the data, the results of these tests are presented in the form of Weibull plots (Figures V.B.1.1 through V.B.1.3) so that a statistical evaluation of the strength of the matrix can be made. The B_{10} and B_{50} strengths of the material were determined from the Weibull distributions and are presented in Table V.B.1.1. It can be seen that the thin-wall matrix exhibits a radial compressive strength considerably less than the radial modulus of rupture.

Specimens are currently being prepared from two other thin-wall AS cores which also experienced ring gear separations. A statistical analysis of the data accrued from these specimens together with the data presented in Table V.B.1.1, should provide a prediction of the variation in matrix strength which may be expected between different thin-wall cores.

V. B.2 Stress Analysis

The critical thermal stress in a regenerator made from a high thermal expansion ceramic material is tangential tension at the hot face outside diameter. Engine operating experience (Task I) has shown that the addition of stress relief slots to the rim of a high expansion regenerator can substantially increase durability.

A simplified (axisymmetric model) finite element stress analysis was previously performed for three configurations of stress relieved LAS regenerators operating at an inlet temperature of 800°C (1472°F) (Reference 1). Each of the three slot con-

figurations had a radial length at the hot face of 25 mm (1.0 in). The slots were tapered axially from the hot face to the regenerator rim. Regenerators incorporating slots with axial lengths of 18 mm (0.7 in), 36 mm (1.4 in) and 71 mm (2.8 in) were analyzed (Figure V.B.2.1). The maximum tangential stresses calculated for an unslotted regenerator and for regenerators incorporating the three stress relief schemes were 1980 KPa (288 psi), 1410 KPa (205 psi), 1040 KPa (151 psi), and 577 KPa (84 psi) with the highest stress occurring in the unslotted regenerator and diminishing with increasing slot depth.

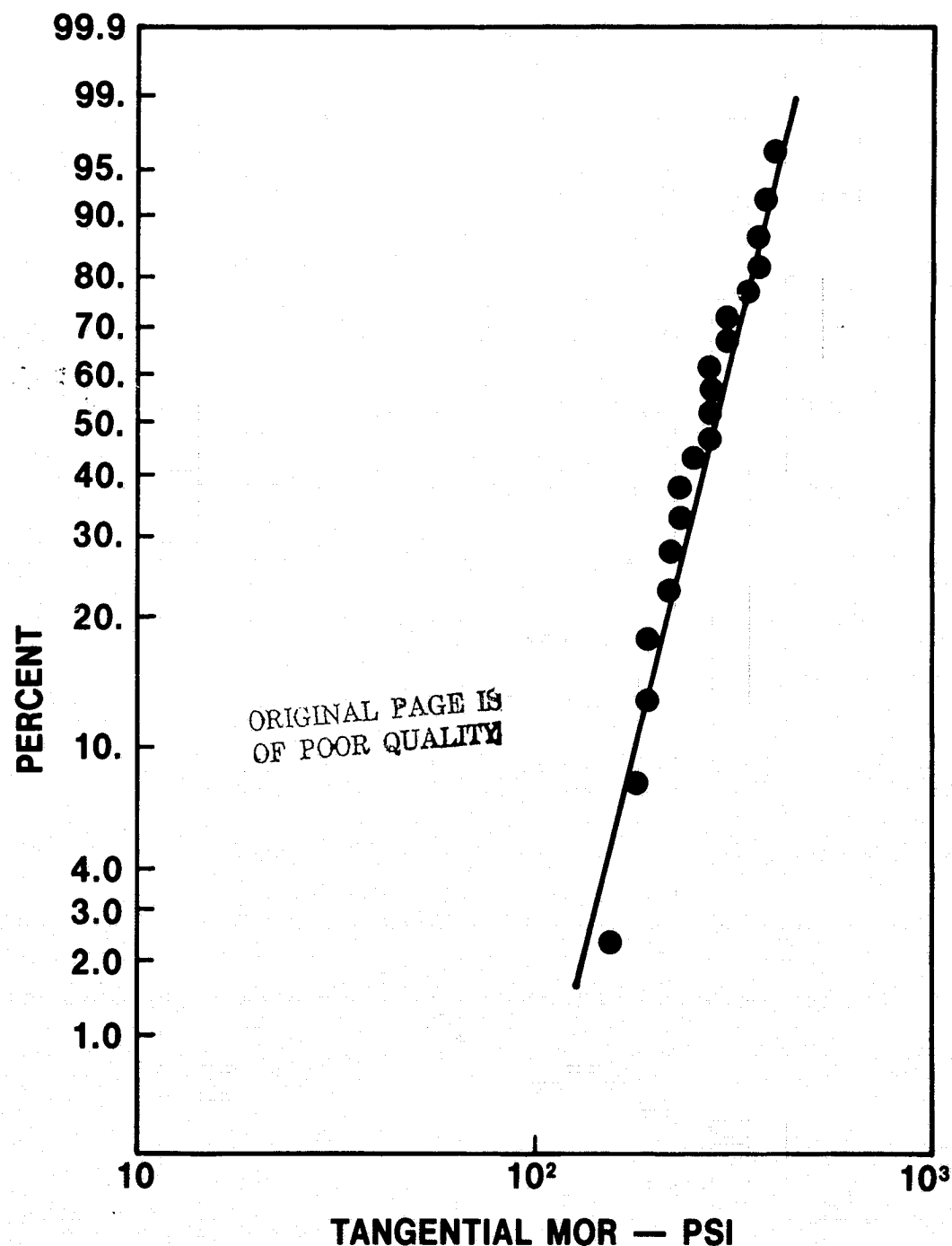


Figure V.B.1.1 Thin-Wall Aluminum Silicate Tangential Modulus of Rupture.

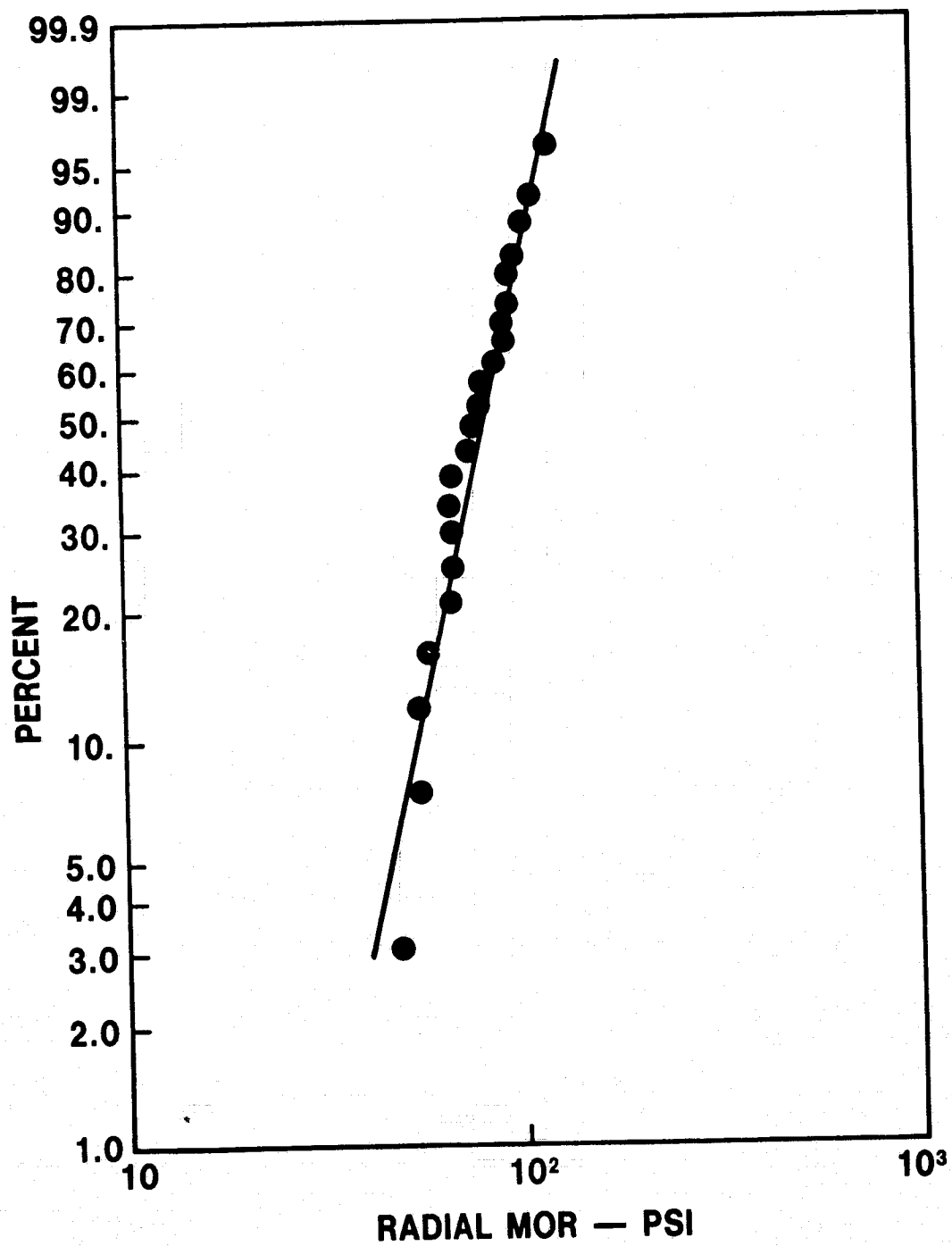


Figure V.B.1.2 Thin-Wall Aluminum Silicate Radial Modulus of Rupture

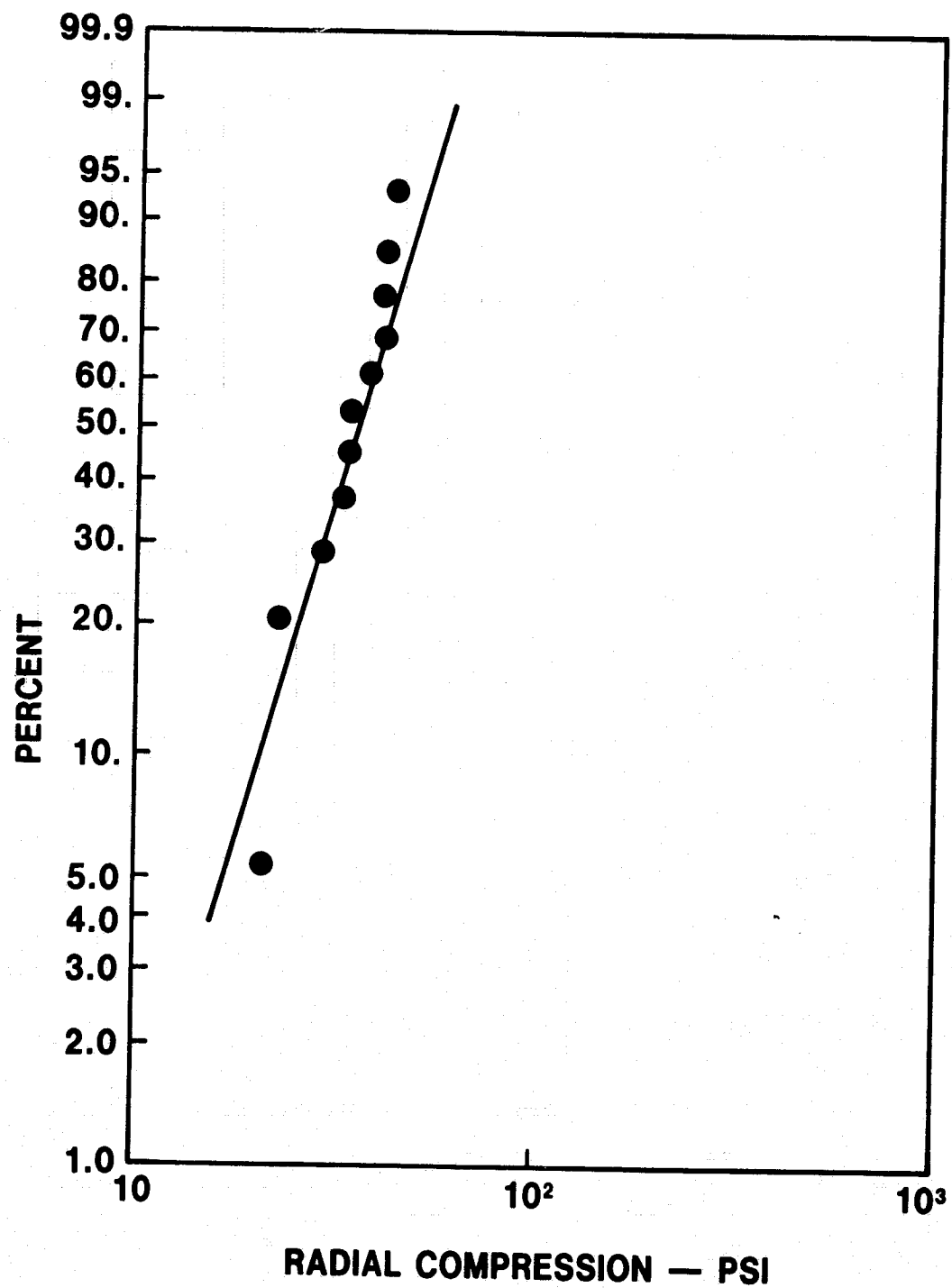
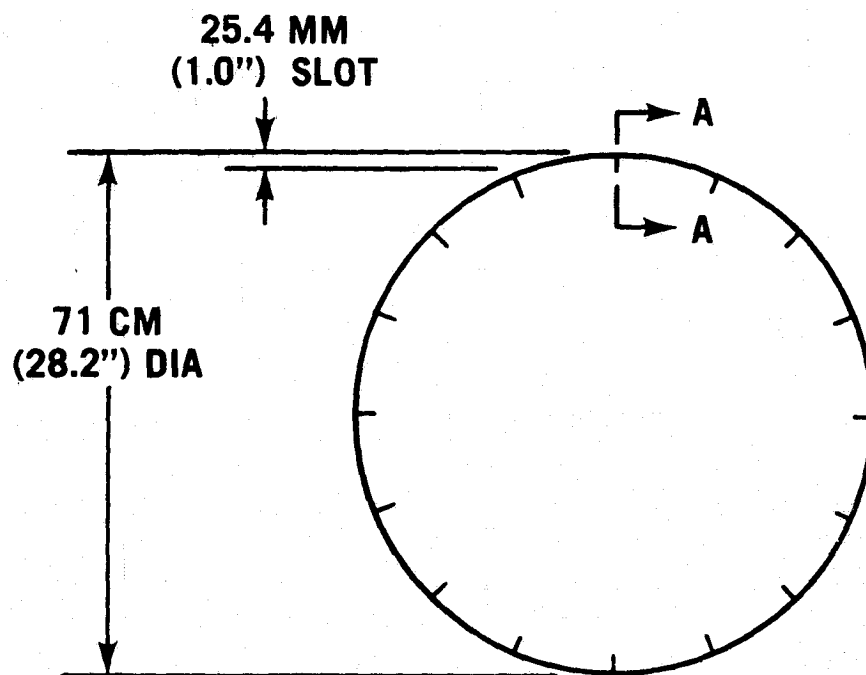


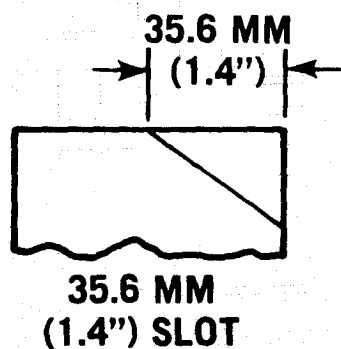
Figure V.B.1.3 Thin-Wall Aluminum Silicate Radial Crush Strength

	TANGENTIAL MODULUS OF RUPTURE KPa (PSI)	RADIAL MODULUS OF RUPTURE KPa (PSI)	RADIAL COMPRESSIVE STRENGTH KPa (PSI)
B10	1240 (180)	372 (54)	152 (22)
B50	1964 (285)	606 (88)	255 (37)

Table V.B.1.1 Thin-wall Aluminum Silicate Material Properties



SECTION A-A, ENLARGED



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Figure V.B.2.1 Stress Relief Slot Configurations

Because the axisymmetric finite element model is essentially that of a regenerator incorporating an infinite number of slots and does not account for stress concentrations, a three-dimensional finite element model has been developed (Figure V.B.2.2). For comparison with the axisymmetric analysis, stress relieved LAS regenerators incorporating 21, 42 and 84 equally spaced 36 mm x 25 mm (1.4 in x 1.0) slots were analyzed at the 800°C (1472°F) operating condition.

The material properties used in both the axisymmetric and three-dimensional analyses are given in Table V.B.2.1.

The three-dimensional finite element analysis indicates that the effect of stress relief slots is to substantially reduce tangential stress in the rim between slots as compared to an unslotted regenerator. High stresses occur at the slots, but diminish rapidly with distance such that their effect is limited to very local areas of the rim in the vicinity of the slot (Figure B.V.2.3). This maximum stress is a function of the number of equally spaced slots around the rim: as the number of slots increases, the maximum stress decreases and approaches the value calculated using the axisymmetric model (Figure V.B.2.4). This indicates that the axisymmetric model can be used to estimate the maximum stress relief obtainable by the addition of slots. The maximum calculated tangential stresses in the rims of LAS regenerators incorporating twenty-one, forty-two and eighty-four slots using the three-dimensional model are 3838 KPa (557 psi), 2281 KPa (331 psi), and 1247 KPa (181 psi) respectively.

The next step in the analysis was to attempt to correlate three-dimensional results with the stress relieved Supplier D MAS regenerator engine operating experience described in Task I, and to evaluate the effect on thermal stress in the MAS core of various slot configurations.

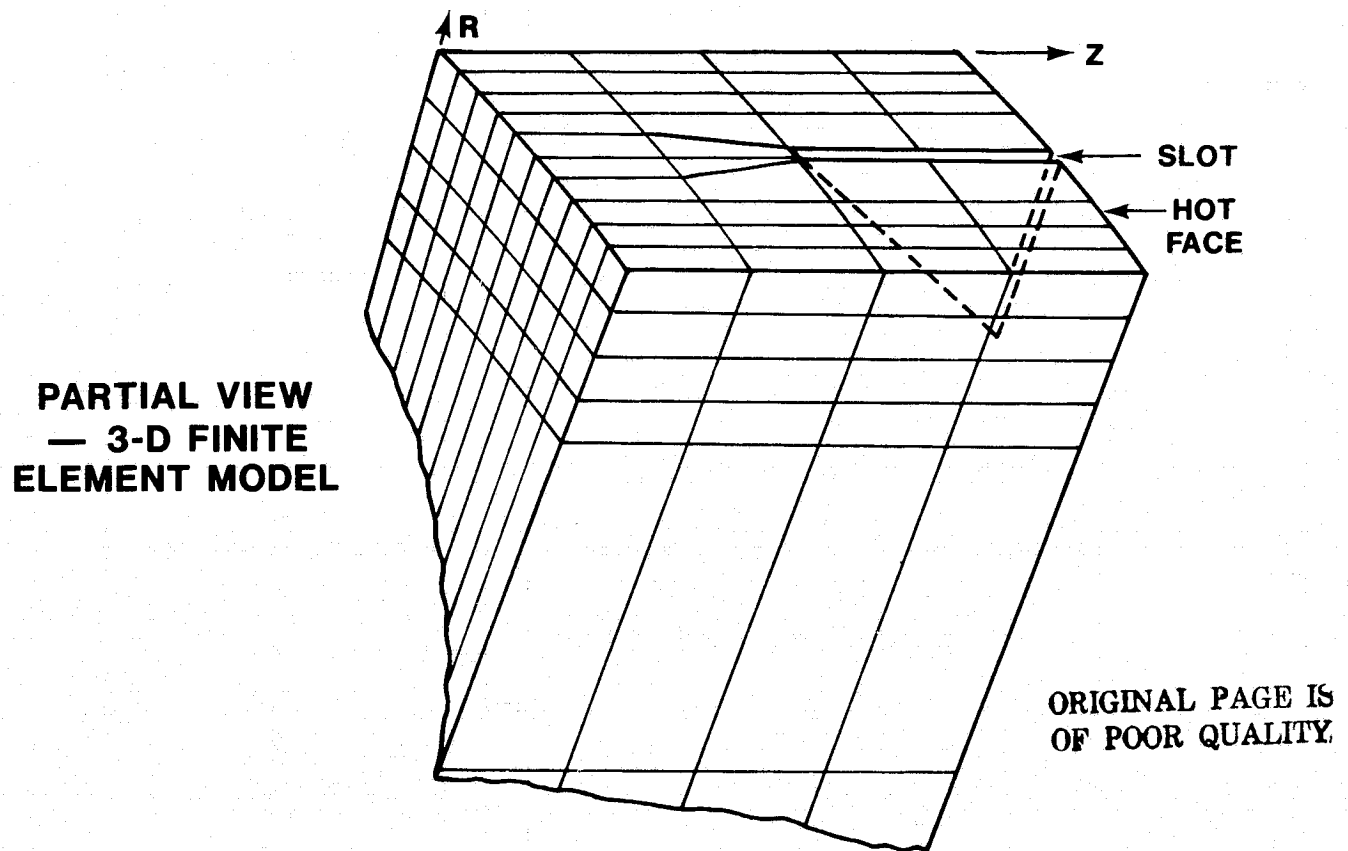


Figure V.B.2.2 Three-Dimensional Finite Element Model of Stress Relieved Regenerator

MODULUS OF ELASTICITY, MPa (PSI)			
RADIAL, E_R		2,068 (300,000)	
AXIAL, E_Z		11,372 (1,650,000)	
TANGENTIAL, E_θ		3,033 (440,000)	
POISSON'S RATIO			
ν_{RZ}		.17	
$\nu_{R\theta}$		.12	
$\nu_{\theta Z}$		.08	
COEFFICIENT OF THERMAL EXPANSION $\times 10^6 \text{ }^\circ\text{C}^{-1} \text{ (}^\circ\text{F}^{-1}) \text{ @ } ^\circ\text{C (}^\circ\text{F)}$			
38°C (100°F)		.27°C ⁻¹ (.15°F ⁻¹)	
204	(400)	.68	(.38)
316	(600)	.94	(.52)
427	(800)	1.17	(.65)
538	(1000)	1.49	(.83)
649	(1200)	1.71	(.95)
871	(1600)	2.03	(1.13)

Table V.B.2.1 Lithium-Aluminum Silicate Material Properties

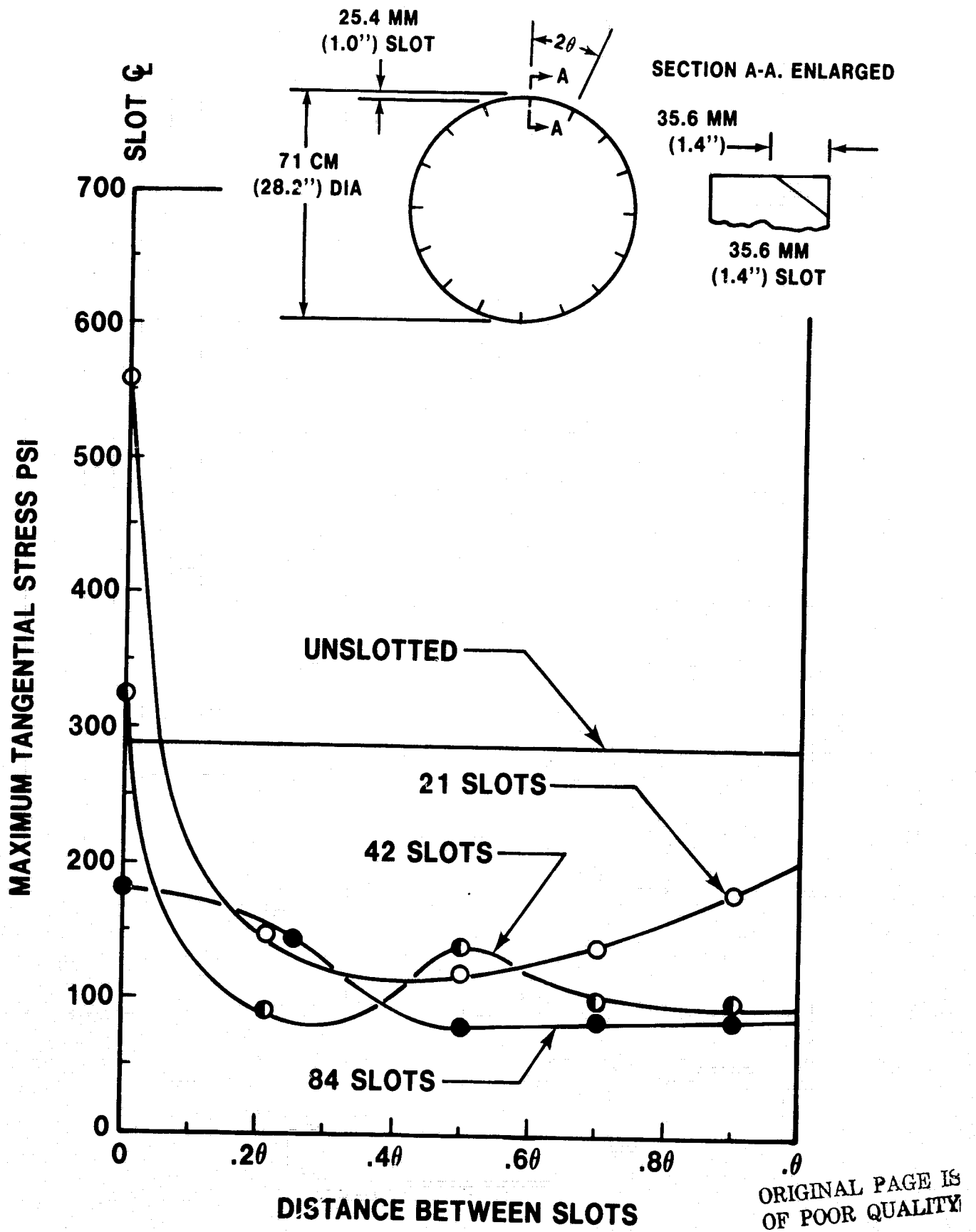


Figure V.B.2.3 Distribution of Tangential Stress in Slotted LAS Regenerator

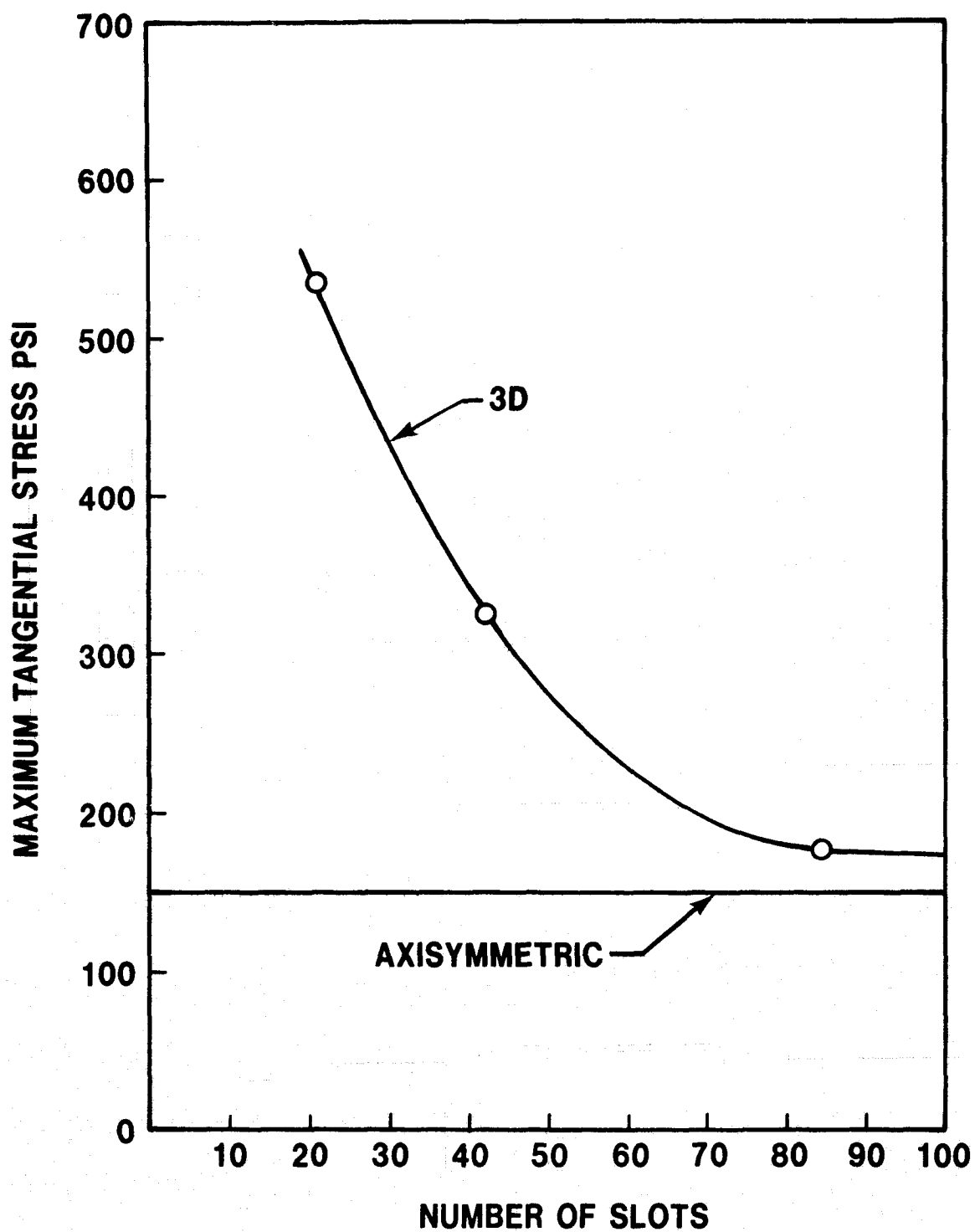


Figure V.B.2.4 Effect of the Number of 36 mm x 25 mm (1.4 inch x 1.0 inch) Stress Relief Slots on Maximum Tangential Stress In the Rim

MAS regenerators were analyzed at the 800°C (1472°F) operating condition for two types of stress relief slots: The standard 36 mm x 25 mm (1.4 in x 1.0 in) slot incorporated in the regenerator undergoing engine test, and a slot extending axially 72 mm (2.8 in), or the full width of the rim. These configurations were analyzed for regenerators incorporating 21, 42 and 84 equally spaced slots. The MAS material properties used in the analysis are given in Table V.B.2.2.

The maximum tangential stress in the rim of an unslotted MAS regenerator was previously calculated to be 3238 KPa (470 psi) (Reference 2.). As with the LAS matrix, the three dimensional analysis showed that the effect of the addition of the slots is to reduce stress substantially (greater than 60%) for areas of the rim between slots (Figures V.B.2.5 and V.B.2.6).

For the 21-slot of the regenerator undergoing engine test, peak stresses of 4465 KPa (648 psi) and 2253 KPa (327 psi) were calculated at the slot locations and mid-way between slots respectively, and thermal stress cracks would be expected at these locations. As described in Task I, cracks did occur between the slots and it is expected that cracks not evident from a non-destructive inspection were generated beneath the slots as well. Cracks generated beneath the slots would not be expected to propagate to the surface of the regenerator due to the state of stress of the core (tangential compression) which exists at the hot face in this region and which would act as a crack arrestor.

MODULUS OF ELASTICITY, MPa (PSI)			
RADIAL, E _R		1,344 (195,000)	
AXIAL, E _Z		20,680 (3,000,000)	
TANGENTIAL, E _θ		11,030 (1,600,000)	
POISSON'S RATIO			
ν _{RZ}		.02	
ν _{Rθ}		.12	
ν _{θZ}		.11	
COEFFICIENT OF THERMAL EXPANSION X10 ⁶ °C ⁻¹ (°F ⁻¹) @ °C(°F)			
-18°C (0°F)		-.49°C ⁻¹	(-.27°F ⁻¹)
204 (400)		-.27	(-.15)
371 (700)		-.11	(-.06)
538 (1000)		.11	(.06)
704 (1300)		.29	(.16)
871 (1600)		.49	(.27)
1038 (1900)		.67	(.37)

Table V.B.2.2 Supplier D Magnesium Aluminum Silicate Material Properties

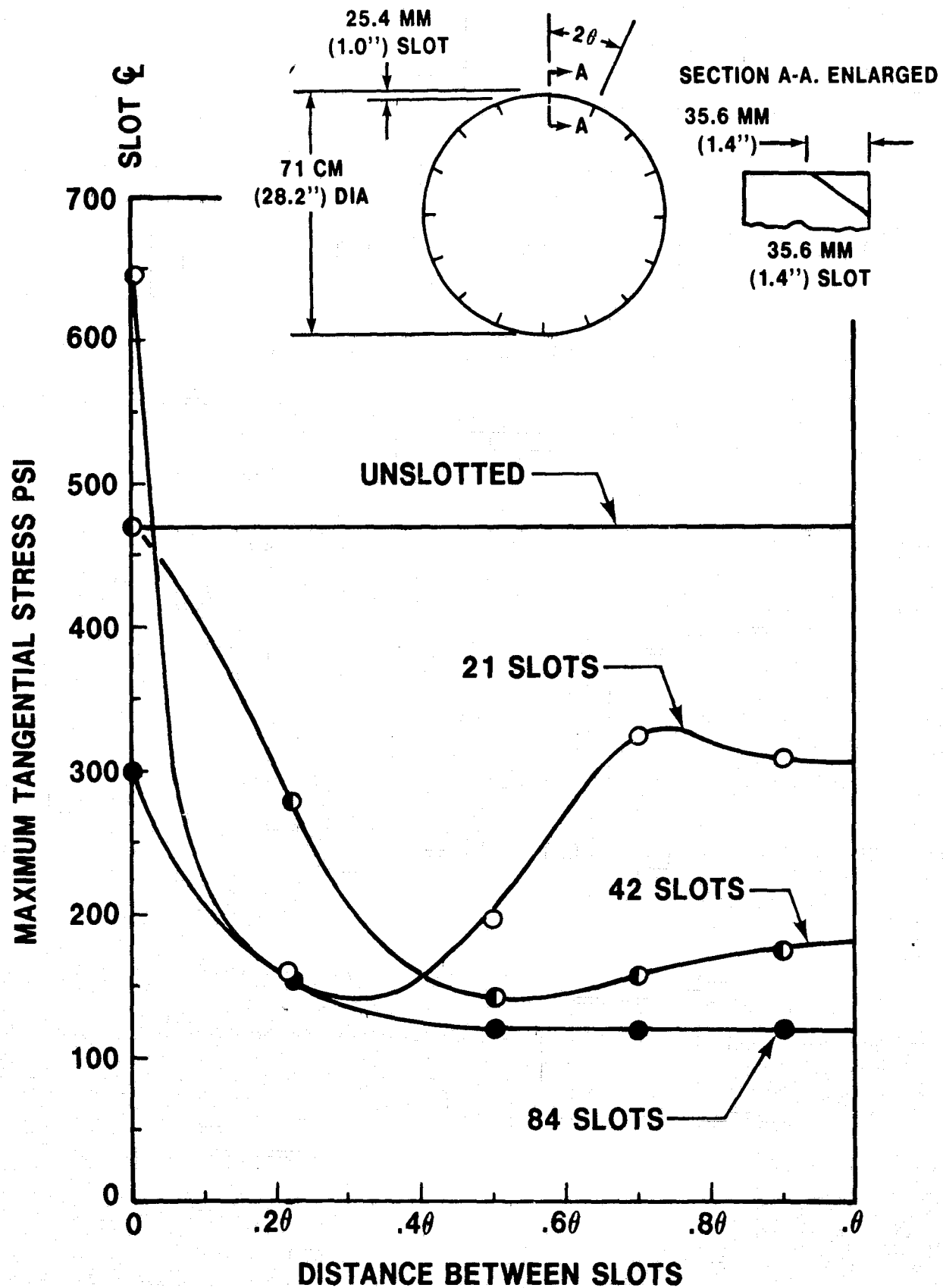


Figure V.B.2.5 Distribution of Tangential Stress in Supplier D MAS Regenerator Incorporating 36 mm x 25 mm (1.4 inch x 1.0 inch) Slots

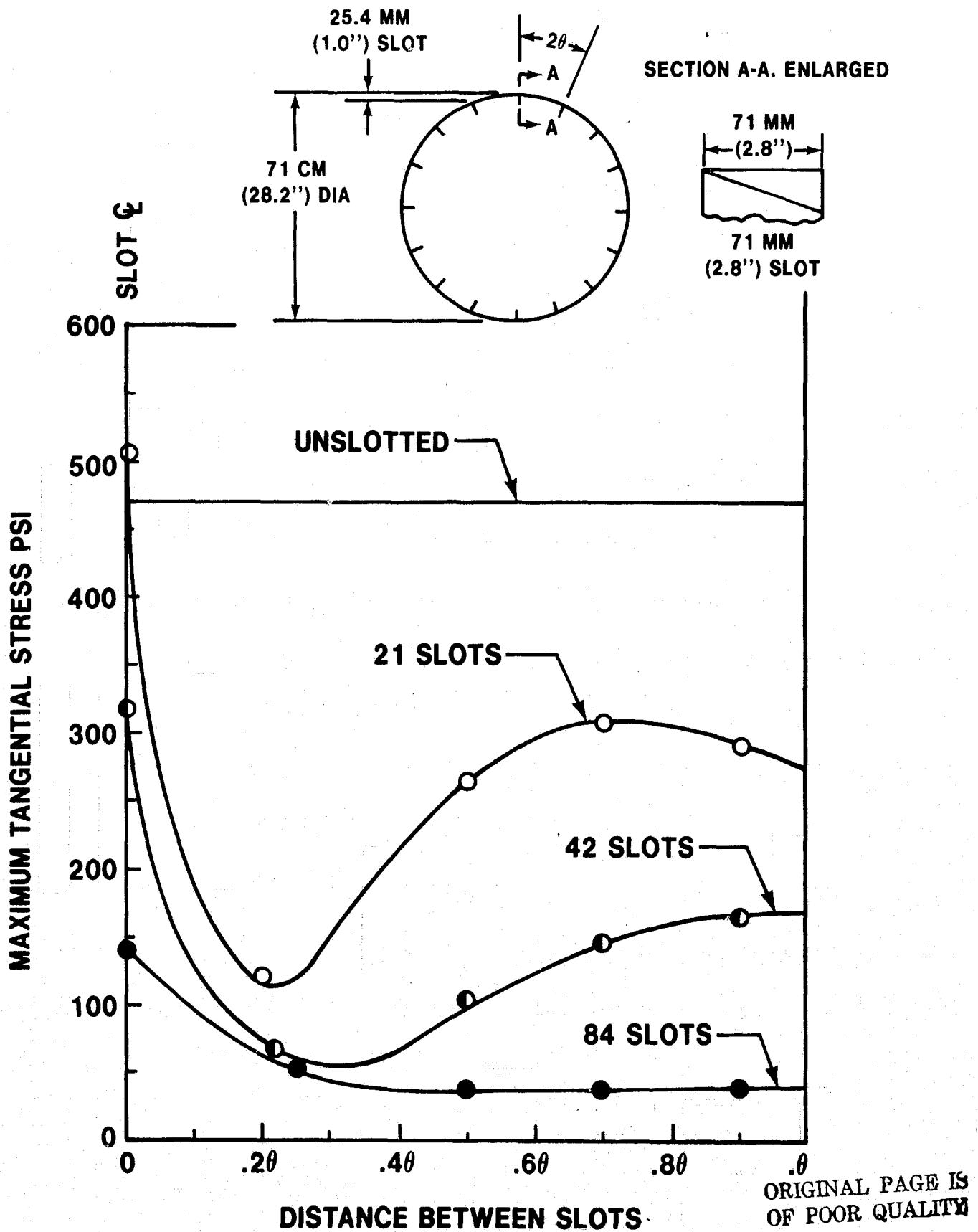


Figure V.B.2.6 Distribution of Tangential Stress in Supplier D MAS Regenerator Incorporating 72 mm x 25 mm (1.4 inch x 1.0 inch) Slots

The effect of increasing the number of slots is to reduce the maximum stress at the slot locations as well as reducing the level of tangential stress between slots. Regenerators incorporating forty-two and eighty-four 36 mm and 25 mm (1.4 in x 1.0 in) slots provide reductions in maximum stress of 27% and 54% respectively over the twenty-one slot configuration.

The analysis of the 72 mm x 25 mm (2.8 in x 1.0 in) slot indicates that a more effective method of reducing thermal stress than increasing the number of slots is to modify the slot geometry. Compared to the analytical results of the baseline 21-slot regenerator like the one operating in the engine, this configuration provides stress reductions of 22%, 51% and 78% for regenerators incorporating 21, 42 and 84 slots respectively.

Although a stress-relieved Supplier D MAS regenerator does not appear suitable for operation at temperatures greater than 800°C (1472°F), the three-dimensional finite element analysis shows that the effective use of stress relief slots can reduce tangential stress in the regenerator rim and provide the capability of higher temperature operation than would be possible with a plain rim regenerator in which the critical stress is tangential tension. Further analysis and engine testing will be required to determine if a modification to the Ford ceramic heat exchanger material specification is justified to take into account stress relieved regenerators made from higher thermal expansion and/or lower strength materials than are currently allowed.

V.B.3 Drive and Mount Analysis

As described in Task I, several failures of thin-wall AS regenerators have occurred which have been attributed to stress imposed on the core (both during the bonding of the gear to the core and during engine operation) due to the differential thermal expansion of the elastomer and ring gear coupled with the low radial compressive strength of the thin-wall matrix. In order to inhibit such failures, compliant elastomer configurations are being investigated for use with the thin-wall aluminum silicate matrix in an attempt to isolate the apparently delicate regenerator core from the mechanical and thermal stresses arising from the drive and support system. Two such schemes were mentioned in Task I and are illustrated in Figure I.B.6.4. One elastomer configuration incorporates about 300 equally spaced axial holes formed by inserting removable tubes in the elastomer prior to curing. It has been estimated that this configuration results in a reduction of the elastomer system apparent radial modulus of elasticity of greater than 25% with a corresponding reduction in radial stress. Another configuration consists of thin elastomer segments bonded alternately to the ring gear and the core, with the segments connected by elastomer beams such that no where around the regenerator rim is the core bonded directly to the gear. This scheme is an attempt to divorce the ring gear from the core (as much as possible), and thus minimize the stresses arising from the differential thermal expansion of the regenerator components.

V.B.4 Seal Requirements

Due to the current interest in increasing the regenerator inlet temperature requirement to 1200°C (2192°F) for future engine applications, the projected requirements for the regenerator seal system must be assessed. In order to project future temperature requirements for the regenerator seal coatings, a preliminary test evaluation of the existing Ford 707 production engine seal system has been completed.

For test evaluation purposes, thermocouples were attached to the edge of the coated substrate (Figure V.B. 4.1) at selected locations to a production inner and outer seal assembly. A schematic of the thermocouple locations is shown on Figure V.B.4.2 with the actual inner and outer seal assemblies shown on Figures V.B.4.3 and V.B. 4.4, respectively.

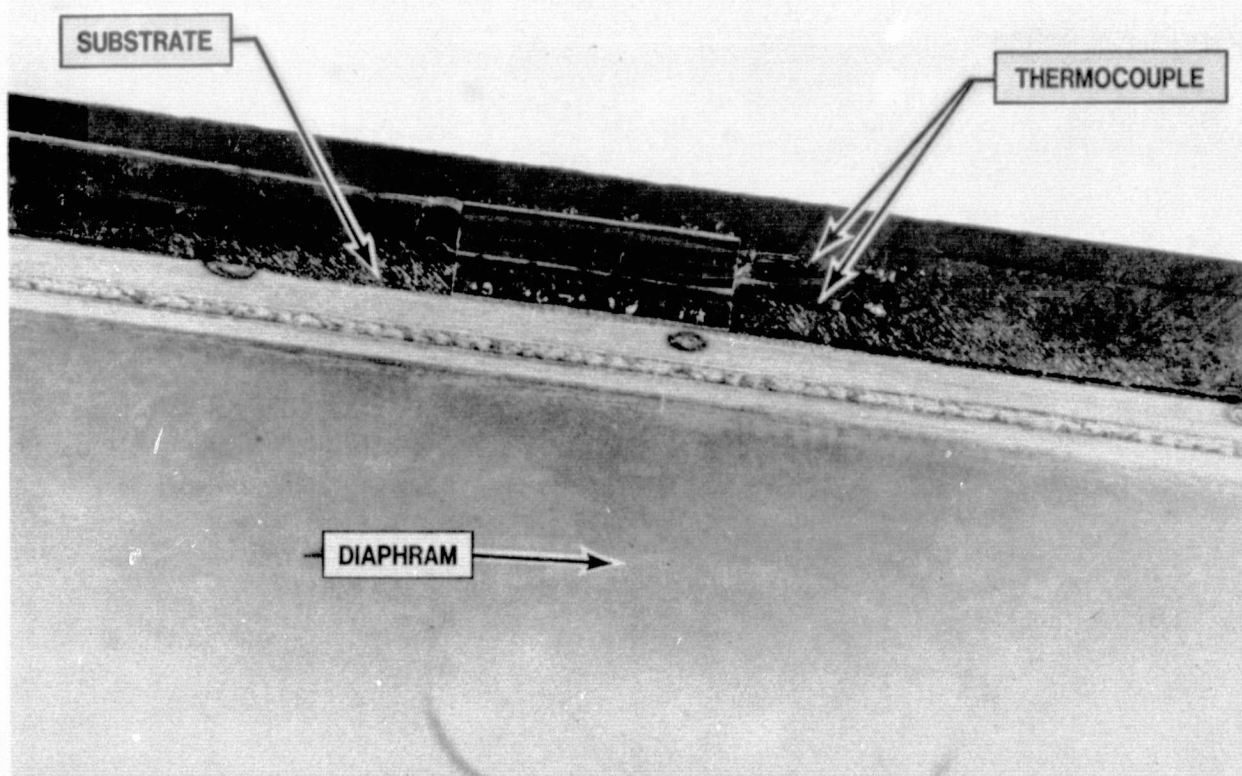


Figure V.B.4.1 Crossarm Seal Probe Mounting Location

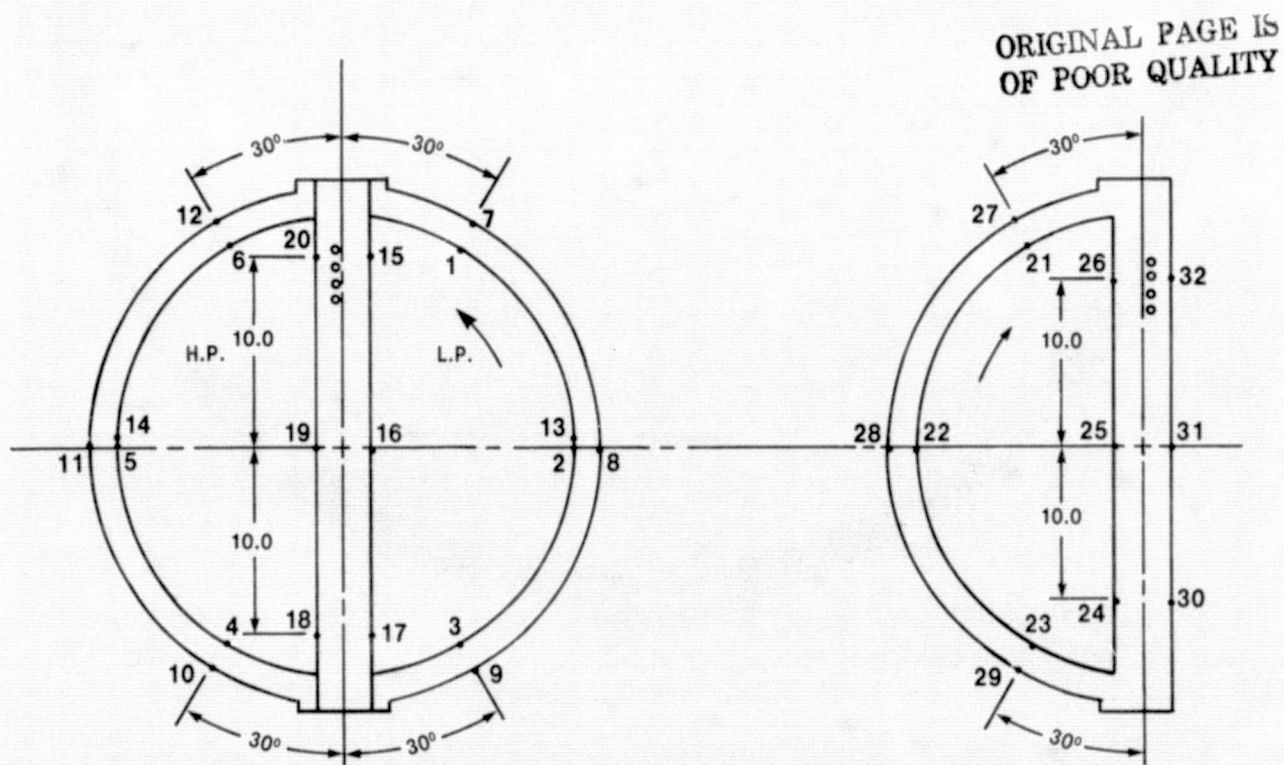


Figure V.B.4.2 Outer Seal Probe Mounting Location

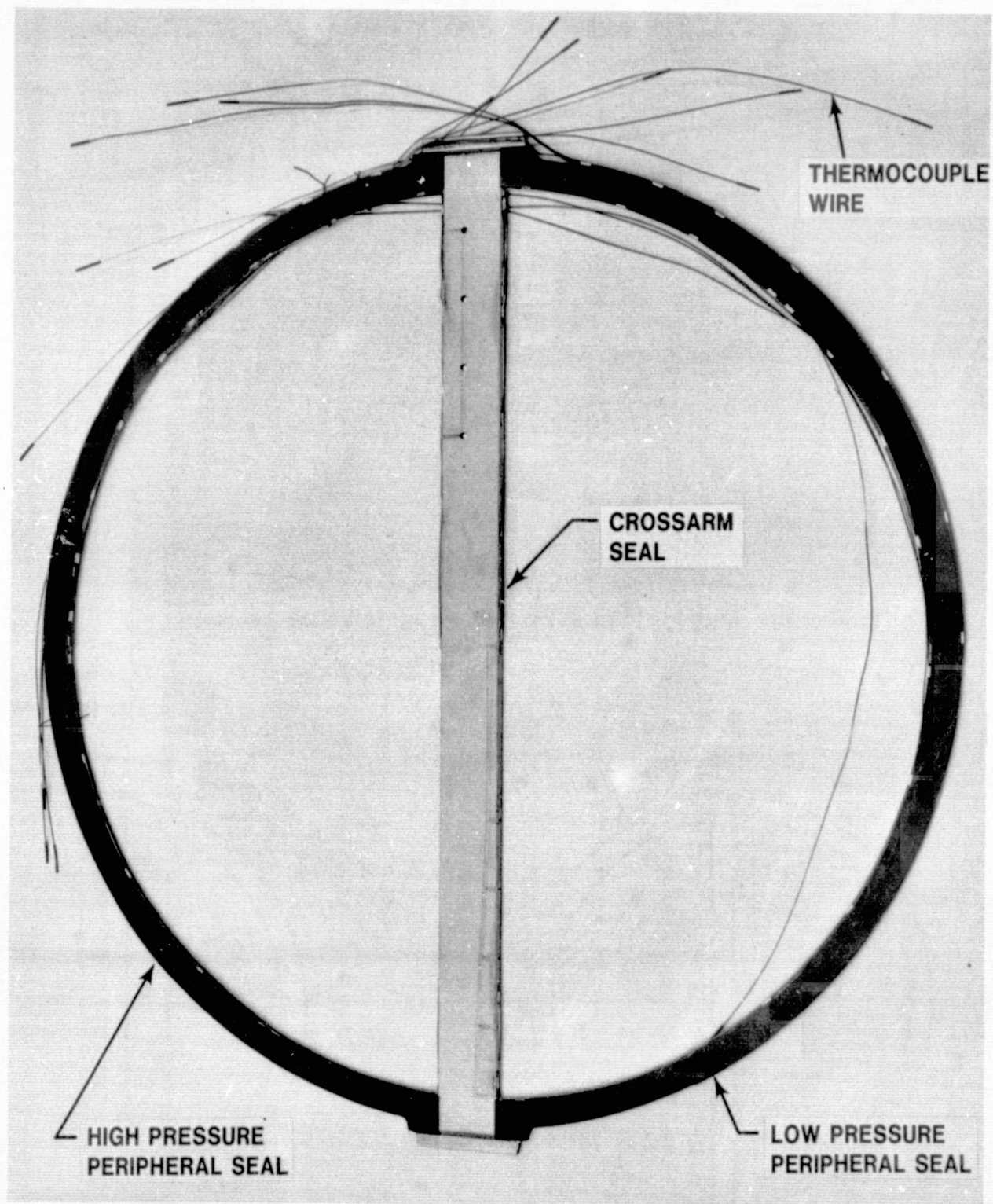


Figure V. B. 4. 3 Instrumented Inner Seal Assembly

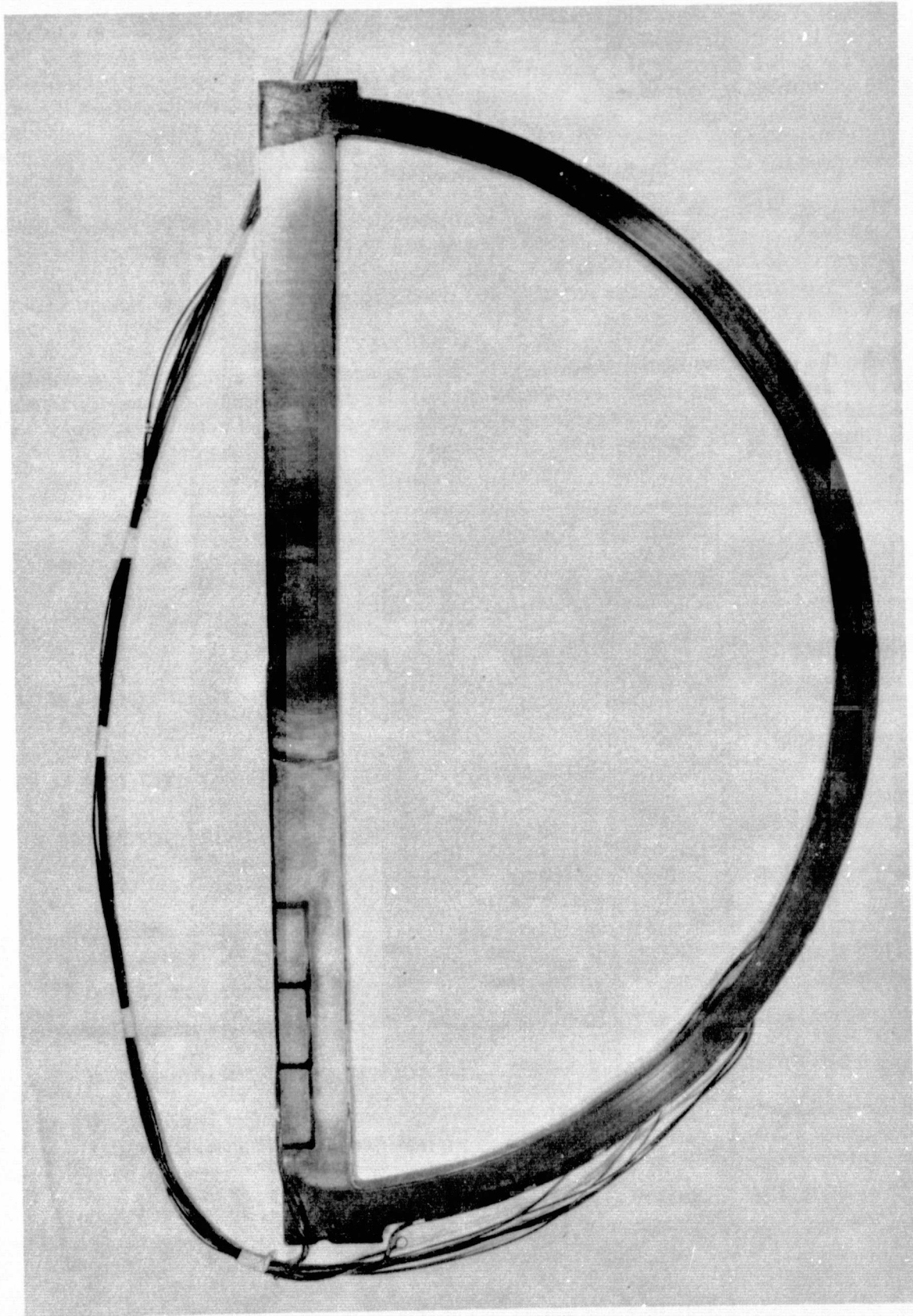


Figure V. B. 4. 4 Instrumented Outer Seal Assembly

The assemblies were then installed in the modified Ford 707 production engine described in Task II, which allows a maximum regenerator inlet temperature of 982°C (1800°F). In the absence of a gas turbine or test rig facility capable of providing higher regenerator inlet temperatures, the measured test data were extrapolated to the desired 1200°C (2192°F) condition. Regenerator seal temperatures were recorded for regenerator inlet temperatures of 871°C (1600°F) to 982°C (1800°F) in 28°C (50°F) increments at a compressor discharge air temperature of 104°C (220°F).

The projected seal component temperatures for a 1200°C (2192°F) regenerator inlet gas temperature with 104°C (220°F) and 232°C (450°F) compressor discharge air temperatures are listed on Table V.B.4.1. Based on these preliminary projected temperatures the status of the existing 707 production engine seal coatings is shown on Table V.B.4.1.

With the exception of the inner seal crossarm location, it appears the current state of the art seal coatings can be substituted at various seal component locations to satisfy projected temperature requirements. More testing is required before the upper temperature limitation of the current inner seal crossarm coating can be defined.

SEAL COMPONENT	CURRENT FORD COATING	L.P. GAS INLET = 1200°C (2192°F.)		ESTIMATED COATING REQUIREMENTS FOR 1200°C APPLICATION
		H.P. AIR INLET TEMP.		
		104°C. (220°F.)	232°C. (450°F.)	
1. INNER SEAL		°C (°F)	°C (°F)	
A. HIGH PRESSURE "C" SHOE	A	125-195 (257-383)	265-330 (509-626)	COATING A IS MARGINAL AT THIS TEMP. REPLACE WITH COATING B
B. LOW PRESSURE "C" SHOW	A	145-335 (293-635)	280-475 (536-887)	COATING A MUST BE REPLACED WITH B AT THESE TEMPERATURES
C. CROSS-ARM	B			
1. H.P. CARRY-OVER		730-955 (1346-1751)	865-1090 (1589-1994)	COATING B MAY BE MARGINAL AT THIS TEMP. A NEW COATING MAY HAVE TO BE DEVELOPED
2. L.P. CARRY-OVER		600-705 (1112-1301)	735-845 (1355-1553)	
2. OUTER SEAL				
A. PERIPHERY	C	115-150 (239-302)	255-290 (491-554)	REPLACE WITH COATING A
B. CROSS-ARM				
1. H.P. CARRY-OVER	C	110-125 (230-257)	250-260 (492-500)	REPLACE WITH COATING A
2. L.P. CARRY-OVER	A	125-420 (257-788)	260-560 (500-1040)	REPLACE WITH COATING B OR DEVELOP NEW COATING

Table V.B.4.1 Seal Temperatures for 1200°C (2192°F) Regenerator Inlet Temperature

V.B.5 The Effect of Thermal Conductivity on Regenerator Design

A primary objective for a vehicular gas turbine is the design of a compact, efficient heat exchanger in order to attain lighter weight and lower cost. Utilization of flow passages with small hydraulic diameters and high surface area to volume

ratio are essential for attaining this objective. Consequently, the regenerator design range for Reynold's No. usually falls within the laminar flow regime with the shape of the heat exchanger possessing a low thickness to outside diameter ratio.

On the other hand, the philosophy for future gas turbine engine designs is to increase the regenerator inlet temperature to the 1100°C (2012°F) to 1200°C (2192°F) range. One of the current tasks of this contract is to determine the upper temperature limit for the present generation of oxide ceramic materials such as magnesium aluminum silicate and aluminum silicate. In the event oxide ceramic materials are not suitable for 1200°C (2192°F) application, existing materials with higher temperature capability such as silicon nitride and silicon carbide must be considered. Unfortunately, these materials possess much higher thermal conductivity, which results in a loss on thermal efficiency compared to the oxide ceramics. For example, the thermal conductivity (KM) of the oxide ceramics are less than .004 cal/cm-sec-°F, (1.0 BTU/hr-ft-°F), whereas the KM of silicon nitride and silicon carbide can vary from .017 to .11 cal/cm-sec-°F (4.0 to 27.0 BTU/hr-ft-°F), depending on the density of the material and the temperature of use. Thus, the present regenerator design philosophy must be adjusted in order to accommodate these materials.

In order to illustrate the effect of thermal conductivity on regenerator design the following example has been selected. A typical 100 hp passenger car gas turbine engine was selected to establish the flow conditions and performance design requirements for the regenerator. For this application the primary engine operating condition for the regenerator is near idle where maximum thermal efficiency (effectiveness) is desired for maximum fuel economy at part power. The effect of thermal conductivity is most severe at this operating condition as illustrated by the following expressions:

$$\epsilon_T = \text{Theoretical Effectiveness} = (U_O, X^*)$$

$$\epsilon_R = \text{Correction Factor Due to Rotation}$$

$$\lambda = \text{Conduction Parameter} = \frac{K_M A_F (1 - \sigma)}{C_{MIN} L}$$

$$\eta_c = \text{Thermal Conductivity Correction Factor} = \frac{1}{1 + \lambda}$$

$$\epsilon_A = \text{Actual Effectiveness} = \eta_c \epsilon_R \epsilon_T$$

$$\text{Where: } U_O = \text{Number of Heat Transfer Units (NTU)}$$

$$X^* = \text{Flow Capacity Rate Ratio, } C_{MIN}/C_{MAX}$$

$$K_M = \text{Thermal Conductivity of the Material in the Axial Direction}$$

$$A_F = \text{Frontal Area of the Regenerator}$$

$$\sigma = \text{Matrix Open Area Ratio}$$

$$C_{MIN} = \text{Minimum Flow Capacity Rate}$$

$$C_{MAX} = \text{Maximum Flow Capacity Rate}$$

$$L = \text{Regenerator Thickness}$$

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Based on the existing matrix sample size (Table IV.B.1 and IV.B.2) the most efficient and compact fin (Core 19) is selected to produce the smallest heat exchanger volume in order to attain the desired performance levels at full power conditions. Utilizing matrix No. 19 a singular regenerator that is 35.8 cm O.D. x 5.1 cm I.D. x 7.1 cm thick (14.1 in x 2.0 in x 2.8 in) will result in an effectiveness of 94.7% with 4.20% pressure drop at full power conditions. The predicted effectiveness near engine idle conditions for this regenerator would then be 99.0%. It should be noted that predicted regenerator performance assumes ideal flow conditions and no leakage, which in effect is the ultimate performance potential.

For illustration purposes matrices 11 and 18 with larger hydraulic diameters than matrix 19 were selected. Regenerators were then sized to attain the same performance level as matrix 19 at full power conditions utilizing these matrices. Once the regenerator size is determined, the performance near engine idle condition is evaluated for comparison with the reference matrix (19).

To illustrate the influence of thermal conductivity, a new regenerator size is determined to attain the required performance level at full power conditions for each matrix geometry for a wide range of thermal conductivities. At each value of thermal conductivity, the effectiveness is then determined near engine idle condition for each matrix regenerator size. Several interesting observations can be drawn from the results shown on Figure V.B.5.1, as follows:

1. In order to maintain the design requirements of effectiveness and pressure drop at full power condition, the outside diameter and thickness must increase proportionately with thermal conductivity for each matrix geometry.
2. As expected, volume requirements to maintain performance levels at full power condition are proportional to the hydraulic diameter of the matrix geometry.
3. Conversely, the effectiveness at engine idle conditions is proportional to the hydraulic diameter of the matrix fin configuration.

For example, at a material thermal conductivity of $.10 \text{ cal/cm-sec-}^{\circ}\text{C}$ ($25 \text{ BTU/hr ft-}^{\circ}\text{F}$) the effectiveness of the matrix 18 regenerator would be 6.7% greater than the matrix 19 regenerator near idle condition. To achieve this gain in effectiveness, the volume of the matrix 18 regenerator would be more than twice that of the matrix 19 regenerator. For the typical 100 hp gas turbine engine selected this gain in effectiveness (67%) would result in a 31% improvement in fuel economy.

The above example has served to illustrate how the present design philosophy may have to be altered to attain future high temperature engine objectives. If higher conductivity materials are required for future applications, then the thickness to diameter shape factor of the regenerator must be increased for each matrix geometry to attain the same performance level at full power conditions. In addition, the regenerator matrix hydraulic diameter would have to be larger in order to attain maximum fuel economy at part power conditions. The net result would be to design future regenerators with smaller frontal area and increased thickness and with matrix geometries with larger hydraulic diameters, which would substantially increase volume.

An additional advantage to this design concept would be the resultant increase in the Reynold's No. operating range. When engine operating conditions for a given size regenerator necessitate a low Reynold's No. condition ($N_{re} < 80$), the heat transfer characteristics are not clearly defined resulting in increased difficulty in attaining ideal flow conditions. In addition, effects due to corners, boundary layer

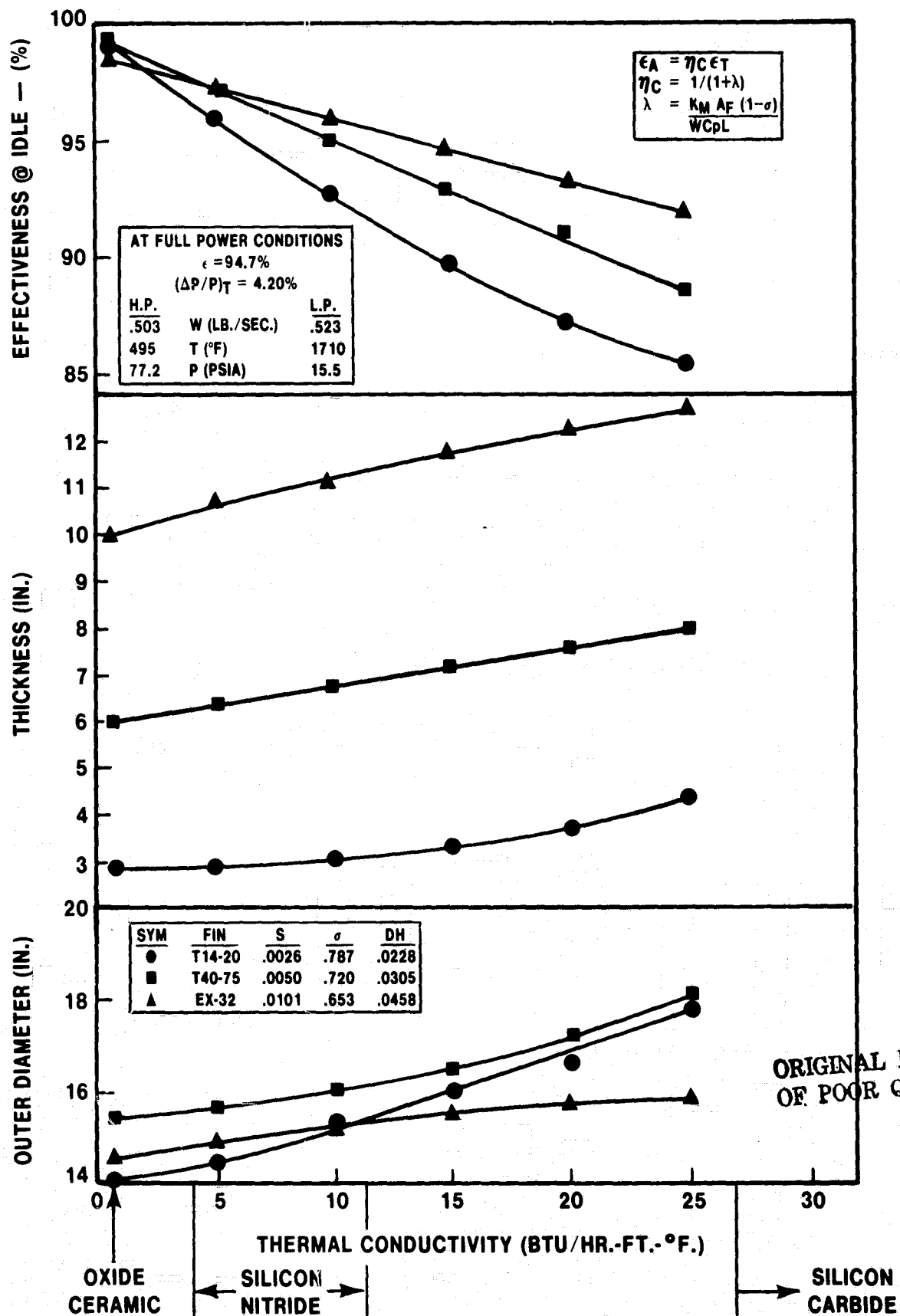


Figure V.B.5.1 Effect of Thermal Conductivity on Regenerator Performance and Size at Engine Idle Conditions

and fin thickness control, which can be detrimental to matrix performance characteristics, are easier to control with less compact ($DH > .025$ in.) fin configuration.

V.C. PROBLEM AREAS

There are no current problems.

V.D. FUTURE PLANS

As promising materials are identified through characterization of their thermal expansion and chemical stability (Task III), mechanical properties will be evaluated and regenerator systems incorporating these materials will be analyzed for structural integrity at the 1000°C through 1200°C (1832°F thru 2192°F) operating conditions.

Development of a method of preventing thin-wall matrix rim compressive stress failure and ring gear separation remains a high priority. Therefore, efforts will be directed toward providing a more compliant elastomer scheme, and towards evaluating the thin-wall matrix material properties in order to more accurately predict structural performance of the thin-wall regenerator. Consequently, characterization of the strength of the thin-wall aluminum silicate matrix will continue in the interest of determining the variation of strength which can be expected from core to core, as well as to provide a better estimate of the strength variation which may be expected within a single core.

V.E. TASK SUMMARY

Matrix mechanical properties (MOR and radial compressive strength) were evaluated for a thin-wall aluminum silicate core. It was determined that the radial compressive strength is substantially less than the radial MOR.

A three-dimensional finite element stress analysis of several configurations of stress relieved regenerators indicated that stress relief slots substantially reduce tangential stress in the rim of a regenerator between the slots.

Work is in progress to determine if modifications can be made to the elastomer bond configuration to enhance AS thin-wall core durability.

A preliminary test evaluation indicates that with the exception of the inner seal crossarm, current state of the art seal coatings appear to be acceptable for 1200°C (2192°F) regenerator inlet temperature operation. Additional testing is required to establish the upper temperature limitation of the crossarm coating. A more refractory material, such as silicon nitride, may be required for regenerators where the inlet temperature exceeds 1100°C (2012°F) or 1200°C (2192°F). Increased axial length and hydraulic diameter would have to be incorporated into regenerators of these materials to control pressure loss and still compensate for the axial conductivity loss inherent with these high conductivity materials.

TASK VI THERMAL STABILITY TESTS OF CERAMICS

VI. A INTRODUCTION

This laboratory testing program is aimed at the evaluation of materials for regenerator core service at temperatures above 800°C (1472°F). Thermal stability testing will serve to place upper temperature limits on the serviceability of present generation matrix materials (for example, aluminous silicate and magnesium aluminum silicate). It is an additional goal of this testing program to evaluate other refractory ceramic materials (perhaps silicon carbide and silicon nitride) that are not now in general use in regenerator applications. This high temperature testing will be carried out, both with and without sodium present, at temperatures of 1000°C (1832°F), 1100°C (2012°F) and 1200°C (2192°F). If a material fails at one of the upper two temperatures, this material will be evaluated 50°C (90°F) lower.

The high temperature dimensional stability of select candidate materials will be evaluated by periodic measurements of specimen length change as a function of time held at the test temperature. The thermal expansion behavior between room temperature and the test temperature will be determined for each candidate material before testing and after completing the prescribed testing schedule. The procedure used to evaluate the high temperature dimensional stability of a material in the presence of sodium is identical to the above-described procedure, except that the samples must be treated prior to the testing program with a sodium-bearing material. This will be accomplished by soaking in a 3.5% sodium chloride solution. A comparison of the data from both tests will point out the propensity of these ceramic materials to suffer corrosion by sodium at these elevated temperatures. As in previous testing, all length measurements are determined using a Sheffield Visual Comparator with 5000:1 amplification. Lengths are measured and reported to the nearest 2.5×10^{-5} mm (one millionth of an inch), and the measurement is reported to be accurate to within $\pm 1.30 \times 10^{-4}$ mm (± 5 millionths of an inch). During the heating to and cooling from the test temperature, rates of less than 50°C (90°F) per minute are maintained in order to eliminate any dimensional changes induced by thermal shock.

VI. B STATUS

VI. B.1 1000°C (1832°F) Test Temperature

Duplicate samples of three oxide ceramic materials, one non-oxide ceramic honeycomb, and a 9455 LAS standard were machined to test specimen shape, cleaned, and placed on test. The machining and cleaning steps have been described earlier, (Report Section III. B. 1) and were reproduced for all thermal stability sample sets.

Figure VI. B. 1. 1 graphically displays the measured changes in length resulting from a thermal soak at 1000°C (1832°F) for periods of time ranging to one week (168 hours). The only material that evidences a significant instability at these early test times is the LAS/MAS composition. The AS material, while exhibiting an early contraction, is behaving as predicted by earlier tests. These data represent relatively short test times compared to the full test term of six weeks, so it is likely that more significant trends of material stability will emerge with increasing test time.

Figure VI. B. 1. 2 represents a similar set of samples, again tested at 1000°C (1832°F), but in the presence of sodium. The corrosive nature of this test is pointed out by the early and continuing growth of the 9455 LAS being used as a reference material. This material has been shown to suffer from a sodium-for-lithium ion exchange, the rate being temperature dependent. The MAS material is quite stable, and this behavior would be predicted from structural considerations. The MAS material holds onto the magnesium ion much more tightly than the LAS material holds onto the

lithium ion. Therefore, less ion exchange results. The LAS/MAS composition seems to be steadily growing under these test conditions, and one would suspect that the vulnerability of this two-phase material is to discrete LAS phase. No performance was predicted for the silicon carbide, and these samples were included in these tests because they represent a material which may have to be considered for future, high-temperature regenerator applications. The AS material has contracted a bit more than anticipated, and it will be interesting to see if this trend continues. Again, it is wise to recognize that, although three data points have been taken, the total test time to date is only one week of a six week test. While data gathered to date are interesting, the trends which will emerge over the final course of these testing programs will be crucial. Other candidate materials are being prepared for test.

VI.B.2 1100°C (2012°F) Test Temperature

Duplicate samples of an AS material have been prepared and are on test without sodium present. Data are very preliminary at this time and will not be included in this report. Other candidate materials are now being prepared for introduction into these test programs.

VI.B.3 1200°C (2192°F) Test Temperature

The only samples tested to date are duplicate specimens of an AS composition, and the testing has been without sodium present. Data are preliminary. Other candidate materials are ready for testing.

VI.C. PROBLEM AREAS

The only problem experienced in the implementation of the thermal stability testing program has been the acquisition of all materials of interest. Many materials have been received during the last quarter, and these materials are ready for introduction into the testing program. Some materials exhibit instability by crowning in an axial direction. This necessarily reduces the accuracy of the length measurement. The testing specification requires specimen removal if physical integrity is lost and will be followed.

VI.D. FUTURE PLANS

During the next reporting period, many new samples will be placed on test. While the tests at 1000°C (1832°F) are well underway, tests at the higher temperatures are just beginning. New materials will be placed on test at all three test temperatures, and identical facilities and procedures will be used to bring the new materials up to the test points at which the existing materials now reside. Negotiations are underway to secure a silicon nitride honeycomb for testing at the highest test temperature. Upon arrival this material will be immediately introduced into the test program.

VI.E. TASK SUMMARY

The high temperature dimensional stability testing of ceramic regenerator materials has been started. The testing programs at 1000°C (1832°F) both with and without sodium are well underway with four test materials and a 9455 LAS standard making up the first test batch. Testing at the higher temperatures has been started in a very limited way; however, a material of high interest (AS) was chosen to initiate stability tests at 1100°C (2012°F) and 1200°C (2192°F) without sodium present. New materials have been received, and preparations for testing have been completed. The thermal stability testing program will be significantly enlarged by the introduction of new materials during the next reporting period.

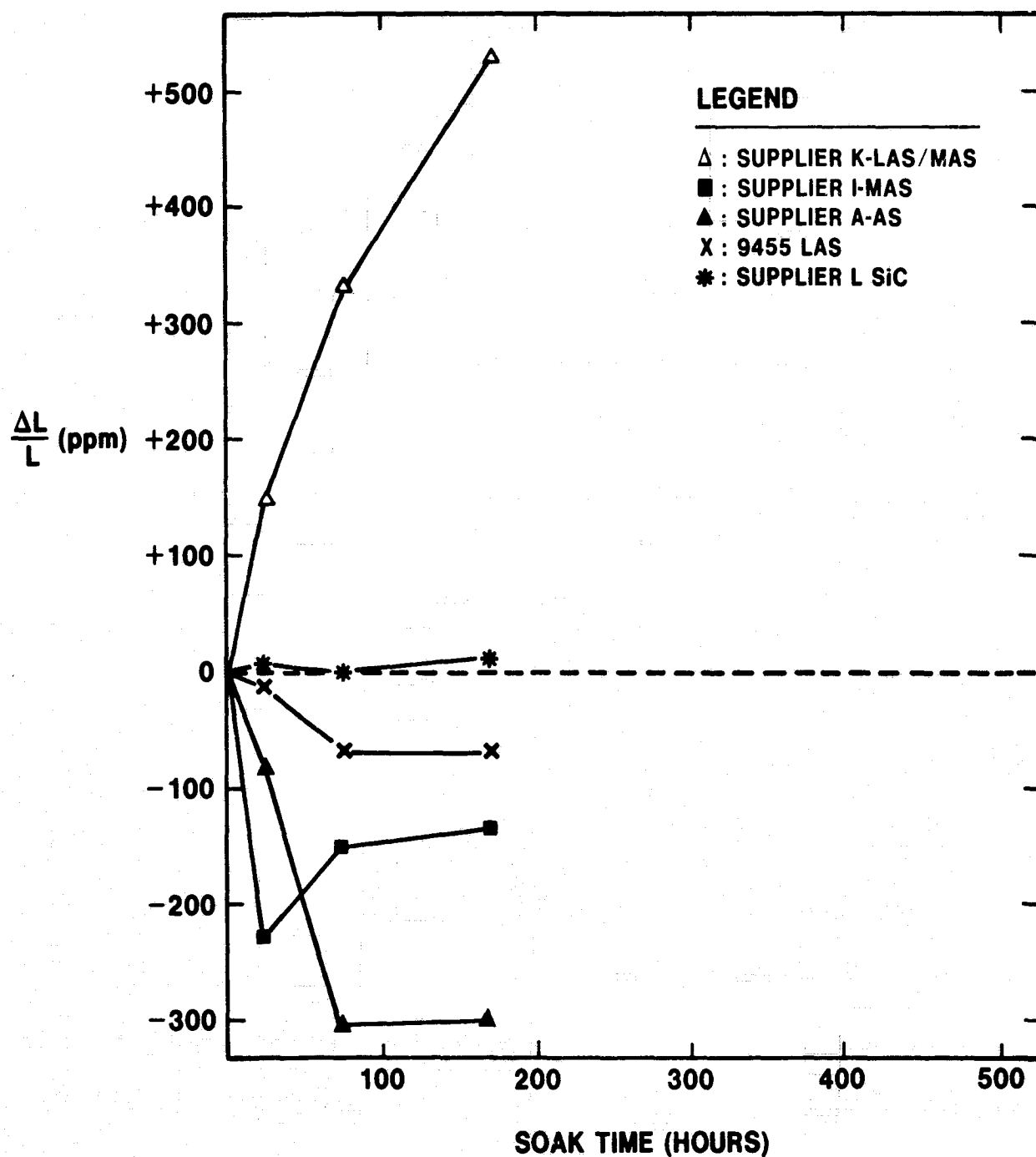


Figure VI.B.1.1 Physical Stability of Various Candidate Regenerator Materials at 1000°C (1832°F)

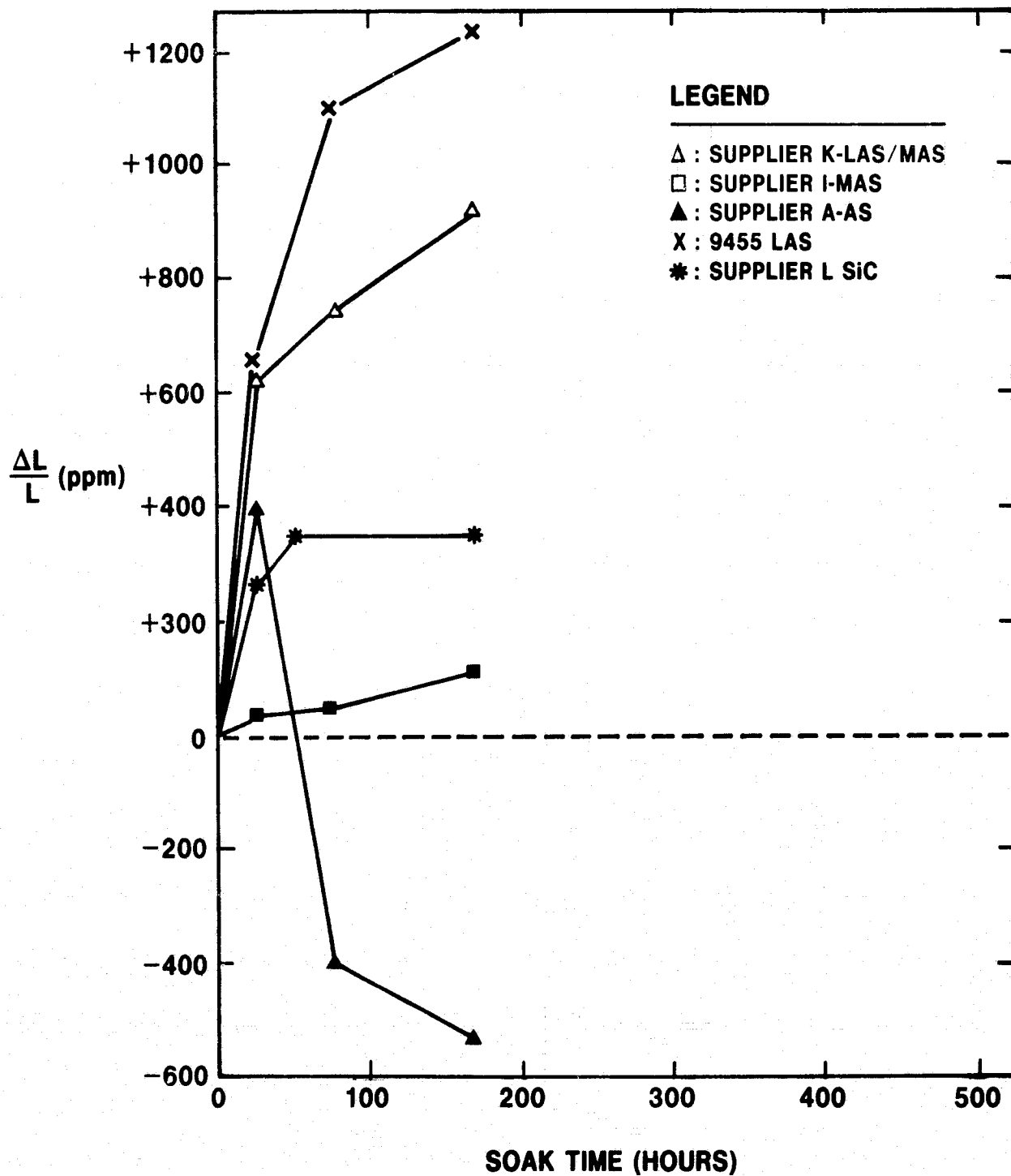


Figure VI.B.1.2 Physical Stability of Various Candidate Regenerator Materials at 1000°C (1832°F) with Sodium

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TASK VII. MANUFACTURING COST STUDIES

VII. A INTRODUCTION

A heat exchanger cost study was initiated at Ford during the last quarter of 1975. Several ceramic companies which had experience in ceramic matrix fabrication contributed to the study. An air preheater, about 46 cm. (18 in) in diameter, fabricated of ceramic material and designed for 1000°C (1832°F) operation in the 170 horsepower Stirling engine, was used as the subject for this study. An annual production volume of 500,000 units was used in the cost analysis. This study concluded with a lowest predicted manufacturing cost range of \$25-\$30 per core. This cost presumed technological developments not now in general practice in the various core production schemes.

Ford, working internally as well as in concert with numerous ceramic manufacturers, will update this existing cost study. This effort will take place on a continual basis to keep the manufacturing cost information as up-to-date as possible. Specific technical innovations, when demonstrated to be cost effective, will be included in the consideration of a manufacturing cost. Periodically, the effect of inflation as well as rising labor and energy costs will be factored into the cost analysis.

VII. B STATUS

Ceramic producers, as well as Ford financial personnel, have been appraised of the plans to update the cost study. Several technical studies evaluating proposed areas of decreasing processing costs are under way at ceramic production sites. The use of cheaper raw materials in the batch composition is being evaluated for both glass and mineral-based MAS materials. To date, no technological breakthrough has been reported that would significantly lower the manufacturing costs of the rotary heat exchanger. The potential cost savings of various batch compositions have not been analyzed, as the large effort has gone into material characterization to determine if specific formulations may be viable regenerator materials. Finally, two very real contributors to the manufacturing cost, inflation and the rising cost of energy, are being assessed. An effort is being made to acquire information from a variety of ceramic producers and combine this with a Ford analysis of the inflationary trends. This approach will introduce the geographic variations in energy costs which exist from plant to plant.

VII. C. PROBLEM AREAS

There are no current problems.

VII. D. FUTURE PLANS

An attempt to assess the contributions of inflation and rising energy costs to the piece cost of the model heat exchanger will be made during the next reporting period. The results of this ongoing study will be reported next quarter. Any technological development, the cost effectiveness having been fully analyzed, which offer potential savings will be reported.

VII. E. TASK SUMMARY

A relatively current (circa 1976) study of the various costs incurred in the production of a ceramic heat exchanger is in existence. The study, representing the combined efforts of Ford and a number of ceramic producers experienced in the fabrication of honeycomb matrix shapes, will serve as a nucleus of information to be periodically updated, reflecting technical innovation as well as increasing costs.

TASK VIII. CORE MATERIAL AND DESIGN SPECIFICATIONS

VIII. A. INTRODUCTION

Two sets of specifications exist for regenerator core material and design. One specification is for a regenerator core suitable for service at 800°C (1472°F), and the other specification is a preliminary attempt to set down the material characteristics and the design considerations critical to a regenerator core assembly expected to function at 1000°C (1832°F).

A very complete purchase specification for regenerator core assemblies intended to operate at 800°C (1472°F) is in existence. This specification resulted from an on-going core purchase, engine testing, and laboratory examination program, and the information so gathered was fashioned into a utilitarian acceptance specification for regenerative heat exchanger assemblies. No additional input is needed to complete this specification; however, new knowledge gained from long-term engine testing, design improvements, and new materials developments has combined to necessitate an interative update of this existing specification.

A very preliminary core material and design specification has been drafted for a regenerator core which would be purposely operated at 1000°C (1832°F). This specification exists in tabular rather than narrative form, as it is essentially a combination of laboratory and design data which represent the initial contemplation of what must be done to raise the operating limit of presently available materials. Basically, this specification originated in a simple attempt to extend the design and materials characterization experience gathered at the lower operating temperature up to the higher temperature regime. In that sense, the 1000°C (1832°F) specification is extrapolative, rather than innovative, in content. As actual engine evaluation is begun, the innovation of techniques will be demanded, and those inputs will be periodically included in the new performance specification.

VIII. B STATUS

VIII.B.1 800°C (1472°F) Specification

This existing specification has received the bulk of Ford's attention during the contract term to date. A thorough renovation of this specification was deemed the more urgent task included in this contractual obligation. To this end, a draft of an up-dated 800°C (1472°F) specification is currently in preparation. This effort is naturally based on the existing specification, and the changes being now incorporated represent knowledge gained from long-term engine testing, continuous laboratory analysis and development, and an active advanced design program. The total contribution of these on-going programs will be to thoroughly refine the existing specification. This effort is well under way.

VIII.B.2 1000°C (1832°F) Specification

Little effort is presently being directed toward the setting down of a definitive material and design specification. Currently, material performance and characterization is being carried out at 1000°C (1832°F) and more data as well as more extensive engine testing experience is needed to knowledgeably draw up even a preliminary materials and design specification. Less attention has been given to this higher-temperature specification, as it was judged pragmatic to progress through an up-dated 800°C (1472°F) specification to the writing of an extended temperature capability specification.

VIII.C. PROBLEM AREAS

There are currently no problems associated with this contractual task.

VII. D. FUTURE PLANS

The original core material and design specification, 800°C (1472°F), is being updated to reflect more modern processing and core preparation techniques, material improvements, and more advanced design understanding. It is anticipated that this task will be completed during the next contractual reporting period. When completed, the revised specification will be reported in the successive quarterly report.

An effort will be undertaken, during the next reporting period, to enlarge the higher temperature design and materials specification, 1000°C (1832°F), to include considerations deemed important in the original drafting of the lower temperature specification. This effort will continue through the subsequent reporting periods, however, progress will be reported on a quarterly basis. The next step in this process will be to draft a more comprehensive specification reflecting the materials and design demands required by higher temperature operation.

VIII.E. TASK SUMMARY

Two ceramic core materials and core design specifications now exist: one for an operational temperature of 800°C (1472°F) and the other, comprised of bits of design and testing data, for 1000°C (1832°F) core temperature. The lower temperature specification is quite comprehensive; however, advancing technology and improved understanding have contributed to the obsolescence of this specification. An update is in order, and this revision is being carried out. The higher temperature specification is quite preliminary, and future attention will dwell on extending the scope of this specification. New materials development and improved design approaches may indicate new thinking in the future formulation of these specifications.

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